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MOLTEN FUEL-MODERATOR INTERACTION: AN INVESTIGATION OF THE POTENTIAL FOR STEAM EXPLOSION

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MOLTEN FUEL-MODERATOR INTERACTION: AN INVESTIGATION OF THE POTENTIAL FOR STEAM EXPLOSION

ABSTRACT

A critical review of the current understanding of vapor explosions was carried out. It was concluded that, on the basis of actual industrial accidents and large scale experiments, energetic high yield steam explosion cannot be regarded as an improbable event if large quantities of molten fuel and coolant are mixed together.

Current understanding suggests that the formation of a coarse fuel fragment-coolant mixture is a prerequisite for energetic steam explosion. Triggering of a local explosion in this mixture and the subsequent propagation of a coherent vaporization wave through the mixture result in the formation of a high pressure steam bubble. Subsequent expansion of this high pressure steam bubble generates shock waves that would subject the structural surroundings to high hydraulic loadings. It is judged that fragment size and spacing are key parameters in establishing the coherent propagation of a vaporisation wave.

This study also reviewed a hydrodynamic transient model proposed by Henry and Fauske Associates to assess a molten fuel-moderator interaction event. It was found that the proposed model negates a priori the possibility of a violent event, by introducing two assumptions: i) fine fragmentation of the molten fuel, and ii) rapid heat transfer from the fine fragments to form steam.

Using the Hicks and Menzies thermodynamic model, maximum work potential and pressure rise in the calandria were estimated. However, it is recommended that a more representative upper bound model based on an underwater explosion of a pressurized volume of steam be developed.

RÉSUMÉ

On a effectué un examen critique des connaissances actuelles des explosions de vapeur. Il a été conclu que, sur la base d'accidents industriels réels et des expériences à grande échelle, les explosions de vapeur qui produisent une grande énergie ne peuvent être considérées comme des situations improbables lorsque sont mélangées des grandes quantités de combustible en fusion et de fluide de refroidissement.

D'après les connaissances actuelles, la formation d'un mélange de fluide de refroidissement et de fragments grossiers de combustible est une condition préalable à une explosion de vapeur énergique. Le déclenchement d'une explosion locale dans ce mélange et la propagation subséquente d'une onde de vaporisation cohérente à travers ce mélange entraînent la formation d'une bulle de vapeur de haute pression. L'expansion ultérieure de cette bulle de vapeur de haute pression produit des ondes de choc qui feraient subir à la structure environnante de fortes charges hydrauliques. On estime que la taille et l'espacement des fragments sont des paramètres clés pour établir la propagation cohérente d'une onde de vaporisation.

La présente étude a examiné également un modèle de transitoire hydrodynamique proposé par Henry and Fauske Associates afin d'évaluer une interaction entre le modérateur et le combustible en fusion. Il a été jugé que le modèle proposé nie a priori la possibilité d'un accident violent, en posant deux hypothèses : premièrement, une fragmentation fine du combustible en fusion, et deuxièmement, un transfert rapide de chaleur à partir des fragments fins pour former de la vapeur.

L'augmentation de pression et le potentiel de travail maximaux dans la calandre ont été évalués au moyen du modèle thermodynamique de Hicks and Menzies. Toutefois, il est recommandé que soit établi un modèle plus représentatif, capable d'atteindre les bornes supérieures de la pression et du potentiel de travail, et basé sur une explosion sous-marine d'un volume de vapeur sous pression.

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PREFACE

This is the final report covering the work performed for the AECB under contract #R1100 342550. The report is divided into two parts. In Part I the current state-of-the-art of vapor explosion is discussed within the context of a global critical analysis of the mechanisms of vapor explosion itself. The objective of Part I is not to provide a technical review of the numerous theoretical models that have been produced, nor an analysis of the experimental results that have been generated in recent years. Part I aims to give the non-specialist a global understanding of the important mechanisms and physical processes involved in steam explosion. Part II of the report provides a detailed critique of a model described in the document entitled "Analysis of Thermal Interactions Following the Hypothetical Severe Blockage of a Coolant Channel" prepared by Fauske and Associates Inc. It was found necessary to go through the model in detail in terms of the assumptions and calculation procedures. The presentation of the criticisms and analysis of the model in Part II of this report follows a step-by-step procedure whereby the model and analysis are first presented and then comments are made. Global remarks on the entire model are also provided at the end. Recommendations, together with a proposal for a model capable of providing a more representative picture of the hydrodynamic transients in case of a steam explosion are also given in Part II of this report.

A final section, in which the Hicks and Menzies thermodynamic model was applied to estimate the maximum work potential and the pressure rise in the calandria, has been added as an Appendix. The work reported in Parts I, II and the Appendix has been presented orally in three separate review meetings with AECB staff. This report is a written documentary of the material presented at these review meetings. The completion of this report constitutes the completion of the final task specified by the contract.

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I. MECHANISMS OF VAPOR EXPLOSION, A CRITICAL REVIEW

1. INTRODUCTION

It is a universally acknowledged fact that a violent destructive vapor explosion can result when masses of hot (fuel) and cold (coolant) liquids are mixed together. The above fact is substantiated by numerous actual accidents in the steel and aluminum, pulp and paper, cryogenic and nuclear industries. There are also numerous small, intermediate and large scale tests using various combinations of fuel-coolant under several initial and boundary conditions that have demonstrated the possibility of violent vapor explosions.

It may be concluded that:

(i) the general requirements for the explosion to occur have been established on a qualitative basis;

(ii) most of the basic physical mechanisms involved have been identified;

(iii) the basic mechanisms themselves are fairly well understood qualitatively on an individual basis, but quantitative description of the non-linear growth processes has not been satisfactorily achieved; and

(iv) the cooperative effects of mutual interaction or coupling between these various basic mechanisms are not well understood even on a qualitative basis. Consequently a convincing proof that a violent vapor explosion cannot occur under certain reactor conditions is difficult to demonstrate. A probabilistic assessment of steam explosion is not too meaningful due to the lack of understanding on a number of key physical processes. However, given a specific accident scenario, it may be possible to go through the chain of possible events and describe on a qualitative basis what can happen, thus upgrading any weak links in the reactor design and operating procedure. Upper limit estimates of the potential damage may also be obtained for specified conditions. But whether these specific conditions can be met in reality is difficult to establish.

The objective of Part I of the report is to give a clear explanation of the different processes and mechanisms of the phenomenon of vapor explosion based on the current state-of-the-art. The sensitivity of the potential for steam explosion to key parameters like mass, temperature and discharge rate

will be evident when the mechanisms are understood. There is a number of excellent reviews on the various aspects of the vapor explosion problem, particularly in the articles by Cronenberg and Benz[1], Zyszkowski[2], and a most recent one by Corradini et al[3]. These reviews are quite extensive covering the details of a wide spectrum of problems. The present report summarizes only the essential mechanisms, aims to provide a global understanding of the phenomenon, and makes general comments regarding the state-of-the-art in vapor explosions.

2. REQUIREMENT FOR VAPOR EXPLOSION

Vapor explosion, also commonly referred to as steam explosion, thermal explosion, molten fuel-coolant interaction (MFCI) and physical explosion (as contrasted with chemical explosion, etc.), refers to the rapid conversion of the cold (volatile) liquid into vapor by heat transfer from the hot liquid with subsequent expansion of the vapor into the environment doing mechanical work and generating shock waves. Figure 1 shows the sequence of events for the idealized vapor explosion.

Consider a mass of liquid (e.g. H_2O) suddenly superheated uniformly to the homogeneous core melt temperature (~3000°K). Molecular fluctuations subsequently will bring about the formation of a large number of nucleation sites in the liquid. Rapid bubble formation from these nucleation sites will result in a phase transition from liquid to vapor. If the phase transition occurs sufficiently rapid without much volume increase, the pressure in the vapor will be very high. Subsequent expansion of the high pressure vapor will do work on the environment (generation of shock waves). There is little disagreement that a violent vapor explosion will result when a sufficiently large mass of liquid is suddenly superheated to a temperature above the homogeneous nucleation temperature. The important question centers around how the cold liquid (coolant) can be superheated rapidly to say the homogeneous nucleation termperature when the thermal energy required for the heating comes from mixing with another mass of hot liquid (fuel). Thus the essential problem in vapor explosion is the rapid mixing problem of two liquids. Furthermore, since explosion is due to the rapid conversion of liquid to vapor, it may not be necessary that the cold liquid (coolant) be heated to the homogeneous nucleation temperature. If heating is via contact with the hot liquid then the

vapor formation rate depends on the total surface area of contact between the two liquids and the heterogeneous nucleation temperature (which depends on the contact angle). Hence it appears from fundamental considerations that vapor explosion would require i) a lower temperature limit (homogeneous or heterogeneous nucleation temperature) for the coolant; ii) a large contact surface area for heat transfer from the hot liquid (fuel) to the cold (coolant) liquid. This implies fragmentation and dispersion of the fragments in the bulk of the coolant liquid; iii) a protection mechanism isolating the thermal energy in the fragments during the dispersion process (i.e., stable film boiling); iv) a triggering mechanism breaking down locally the thermal isolation to permit rapid heat transfer from fragments to coolant in a direct liquid-liquid contact; and v) a propagation mechanism of the "vaporization front" from the local trigger site to the rest of the volume of coolant-fuel fragment mixture. Depending on initial and boundary conditions, not all of the above five requirements are of equal importance. For example, if the hot fuel fragments are "explosively dispersed" it may not be necessary to have the thermal isolation requirements since the heat losses during the very rapid dispersion may be negligibly small if the dispersion time is very short. Also a triggering mechanism may not be necessary if the vapor film is highly unstable. Thus collapse of the vapor film resulting in liquid-liquid contact will occur spontaneously throughout the bulk of the mixture. In general, the five requirements stated above cover the essential steps for vapor explosion to occur.

3. HOMOGENEOUS AND HETEROGENEOUS NUCLEATION OF A LIQUID

Vapor explosion requires a rapid phase transition of the coolant from liquid to vapor. If vapor is formed from nucleation sites in the bulk of the liquid, this is referred to as homogeneous nucleation. However, if the vapor is formed from sites at the contact surface between the hot and the cold liquid, then this is referred to as a heterogeneous nucleation process. It is important to state briefly, then, the two nucleation processes. Consider the following simple experiment in which a liquid is held at a constant temperature in a heat bath. If we decrease the pressure slowly, (i.e, isothermal depressurization) then the liquid expands. But the isothermal compressibility ($K_T = -\frac{1}{v}(\frac{\partial v}{\partial p})_T$ of liquids is extremely small ($0[10^{-6}]$) in general. Thus the

volume change is very small as the pressure is decreased. This phenomenon is illustrated in Fig. 2. Following a given temperature T (isotherm), the P-v relationship goes from A⟶B during the de-pressurization of the liquids. At some pressure P_S (the saturation pressure), the liquid starts to vaporize and the increase of specific volume raises the piston on top of the liquid. Evaporation continues at the constant saturation pressure P_S until all the liquid has turned to its vapor state (C). Further de-pressurization of the vapor follows the curve from C to D. Hence the region A-B is a liquid state, C-D is a vapor or gaseous state, while B-C is two phase, a liquid-vapor mixture. It is possible (e.g., if depressurization is done very gently and the liquid is de-gassed and pure) to depressurize the liquid past the saturation state P_S. The liquid remains in its original liquid phase past the saturation state B until some critical value (B'). The liquid state between B and B' is called the superheated liquid state. It is a metastable state, and a finite perturbation can result in the spontaneous formation of nucleation bubbles and partial vaporization of the liquid bringing about a two phase equilibrium state (back onto the horizontal line B-C). The pressure at B' is the limit (at the given temperature T) in which the metastable liquid state can exist. If one carries out similar experiments at a different temperature T, one can trace out similar saturation states (i.e., B and C) as well as the superheat limit B'. Joining these saturation states gives the classical dome-shaped curve and the so-called spinoidal curve for the superheat limit. Note that one could also have the metastable subcooled vapor state by isothermal compression of a vapor past the saturation state C.

Similarly we could perform an isobaric (constant pressure) heating experiment. Following an isobar, heating brings the liquid from state A to B (see Figure 3), the saturation state. Further heating brings about vaporization and an equilibrium two phase mixture on the horizontal line B-C. However, it is again possible to have a metastable superheated liquid state past the equilibrium saturation state B. There again exists a superheat limit B' or a superheat limit temperature T_{SL} (i.e., the maximum temperature at which a liquid can exist as a metastable superheated liquid). The experiment to determine the superheat limit temperature T_{SL} is to use a differentially heated vertical liquid column with increasing temperature upwards so that the column is stable (Figure 4). Under a constant pressure P, a small drop of the

volatile test liquid (~2.5mm diameter) is introduced at the bottom of the vertical heated column and the density of the liquids is chosen such that the droplet rises very slowly in the heated column. It is assumed that the liquid droplet takes on the local temperature as it rises. Due to lack of heterogeneous sites the liquid droplet is heated to the homogeneous nucleation temperature before it spontaneously undergoes transition to the vapor phase. The superheat limit temperature T_{SL} is thus the homogeneous nucleation temperature of the test liquid. It should be noted that from thermodynamics an approximate formula can be obtained for the superheat limit or the homogeneous nucleation temperature T_{SL} or T_{HN}, i.e.,

$$T_{SL} = T_C\ (0.11\ P_r + 0.89)$$

where T_C is the critical temperature of the liquid, and $P_r = P/P_C$ where P_C is the critical pressure. For water where $T_C = 647.3^oK$, $P_C = 22.09$ MPa, the T_{SL} obtained for a pressure of 100 kPa is about 576.4^oK or about 303^oC.

An alternate method for estimating T_{SL} (or T_{HN}) is based on kinetic theory where the nucleation rate is given by

$$J = Nf\ e^{-W/kT}$$

with $W = \frac{4}{3}\pi r^3(P_L - P_V) + 4\pi r^2\sigma$; N is the number density of molecules in the superheated liquid, f is a "frequency factor" of the order of 10 sec^{-1}. For a bubble of critical size (i.e., r_c), then the internal pressure forces balance the surface tension forces (i.e., $\pi r_c^2\ (P_V - P_L) = 2\pi r_c\sigma$), we find $r_c = \frac{2\sigma}{P_V - P_L}$ and the nucleation rate of critical size bubbles becomes

$$J = Nf \exp\left[- \frac{16\sigma^3\pi}{3(P_V - P_L)^2} \Big/ kT \right].$$

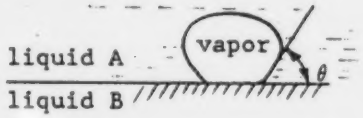
where k is the Boltzmann constant and P_V and P_L denote the pressure inside the bubble and in the liquid respectively.

The above formula does not give an absolute value for T_{SL}, but allows one to determine the probability rate of formation of critical size nucleation bubbles as a function of temperature and pressure. If the rate is very low one can say no nucleation occurs and if the rate is very high one assumes that T_{SL} is exceeded. A plot of the nucleation rate for a critical size embryo as a function of temperature is schematically shown in Figure 5. An approximate value for T_{SL} can be obtained as

$$T_{SL} = \frac{16\pi\sigma^3}{3(P_V - P_L)^2 k}$$

It is clear that if a liquid (coolant) can be brought to the superheat limit at which point homogeneous nucleation sets in, the liquid will spontaneously turn to vapor and the rapid expansion of the vapor will generate shock waves and do mechanical work (structural damage). Thus a sufficient requirement for vapor explosion is the heating of the coolant to the superheat limit for spontaneous homogeneous nucleation to set in.

The important question that follows is "How does the coolant become superheated?" For the reactor problem, the heating of the coolant is via contact with the molten fuel fragments. Thus heat transfer across the interface leads to heterogeneous nucleation since the contact surface enhances the probability of forming critical size nucleation embryos. The heterogeneous nucleation temperature T_{SN} (SN denotes surface nucleation) depends on the contact angle between the two liquids. For a complete wetting fluid where the contact angle $\theta = 0$, $T_{SN} = T_{HN}$ because the heterogeneous surface nucleation rate is the same as the homogeneous bulk nucleation rate. For non-wetting fluids where $\theta = 180^{\circ}$ the possible superheat is zero and $T_{SN} = T_{sat}$. The heterogeneous nucleation rate can be written as



$$J = Nf \exp -\left(\frac{Wf(\theta)}{kT}\right)$$

$$f(\theta) = \frac{2 + 3\cos\theta - \cos^3\theta}{4}$$

and the rest of the symbols are as defined previously in the homogeneous nucleation rate formula. For an ideal wetting surface, $\theta = 0$, $f(\theta) = 1$ and $T_{SN} = T_{HN}$. Thus, depending on the properties of the liquids, the heterogeneous nucleation temperature ranges from the saturation temperature to the homogeneous nucleation temperature (i.e., $T_{sat} < T_{SN} < T_{HN}$).

Based on the above discussions, Henry and Fauske[4] formulated a criterion for vapor explosion. From a simple transient heat conduction calculation, the

contact temperature T_I at the interface between two liquids is given by

$$T_I = \frac{T_H\beta_H + T_C\beta_C}{\beta_H + \beta_C}$$

where T_H, T_C denote the temperatures of the hot and cold liquids respectively and $\beta = K/\sqrt{\alpha}$ where K is the thermal conductivity and α is the thermal diffusivity with the subscript "H" or "C" representing the hot and cold liquids respectively. It seems reasonable to assume that $T_I > T_{SN}$ if vapor explosion is to occur. For typical temperatures of molten fuel and coolant of $T_H \simeq 3000^oK$, $T_C \simeq 300^oK$, the interface temperature $T_I \simeq 1500^oK$. This is far in excess of the homogeneous nucleation temperature of water of about 576^oK. Thus the Henry-Fauske criterion is always satisfied in general in nuclear reactor core melt accident scenarios.

4. THE LEIDENFROST EFFECT

If $T_I > T_{SN}$, we see that a vapor film is quickly formed at the interface between the hot and cold liquids. The cold liquid is now insulated from the hot liquid by the vapor film since the thermal conductivity of the vapor is much less than that corresponding to the liquid. The minimum value of the interface temperature T_I where a stable vapor film can exist is usually referred to as the Leidenfrost temperature T_ℓ. The formation of the vapor film insulating the two liquids preventing direct liquid-liquid contact and hence a large heat transfer rate is called the Leidenfrost effect. Consider the following simple experiment where the heat flux from the hot surface is measured as a function of the temperature difference between the hot and cold liquids (i.e., ΔT), referring to Figure 6. For very large temperature differences, a stable vapor film is formed insulating the two liquids and the heat flux is denoted, say, by point A. When ΔT decreases, the heat flux from the hot surface decreases slightly due to the decrease in the temperature gradient. However, the heat transfer rate is relatively low because of the existence of the stable vapor film. When ΔT decreases to some critical value (point B), the heat flux starts to increase sharply. This is due to the breakdown of the vapor film bringing about periodic liquid-liquid contact and a high heat transfer rate. Violent unstable film boiling heat transfer will occur past point B, which is called the Leidenfrost point, and T_I at this

condition B corresponds to the Leidenfrost temperature T_ℓ. The heat flux reaches a peak at point C and a further decrease in ΔT will result in a decrease of the heat flux again. Past point C, the condition is referred to as the nucleate boiling regime with continuous bubble formation at the interface. The heat flux decreases with further decrease in ΔT in the nucleate boiling regime until at point D, where normal conduction and convection heat transfer commence. The peak heat flux corresponds to the transition from violent unstable film boiling to nucleate boiling. The unstable violent boiling regime starts at the Leidenfrost point B. For an interface temperature $T_I > T_\ell$, we see that a stable vapor film always exists. From thermodynamics, an approximate expression for the Leidenfrost temperature is given[5]

$$T_\ell = 27/32\ T_C$$

where T_C is the critical temperature. Better correlation formulas are given by Baumeister and Simon[6]. For water $T_\ell \approx 423^\circ K$, the lower limit for T will be the saturation temperature for poorly wetted systems.

For vapor explosion to occur it is reasonable to assume that the surface temperature of the hot fuel fragments must exceed the Leidenfrost temperature. A stable vapor film is then present insulating the hot fuel fragments during its dispersion in the coolant. If T_I is not greater than T_ℓ, the heat transfer rate is too high and excessive heat loss occurs during the fragmentation and dispersion processes. We see under typical reactor core melt conditions, $T_I \approx 1500^\circ K$ and since $T_\ell \approx 423^\circ K$, stable vapor film boiling can always occur.

5. FRAGMENTATION MECHANISMS

Given a mass of hot fuel, it is reasonable to assume that it must break up into numerous fragments in order to increase the total heat transfer rate to the coolant for vapor explosion. The heat transfer rate depends on the surface area while the thermal energy content of a fuel fragment depends on the volume. Thus the heat transfer rate per unit volume of fuel is inversely proportional to the size of the fragment "ℓ". The smaller the fragment, the more rapid will be the heat transfer rate necessary for explosive vaporization of the coolant. The basic question is, "How does a mass of molten fuel fragment?" or, more precisely, "What are the mechanisms for fragmentation?"

Fragmentation requires energy, the energy must come from the potential energy of the fuel if it is poured or dropped from a height, or from the thermal energy of the hot molten fuel itself.

If poured from a height, the potential energy is converted into kinetic energy and we have essentially a jet of hot molten fuel penetrating into the liquid coolant. Break up of the liquid fuel jet is governed by a Weber number criterion which essentially states that the ratio of inertia to surface tension force should exceed a certain critical value for break up of the jet to occur. The Weber number W_e is defined as

$$W_e = \frac{\text{inertia force}}{\text{surface tension force}} = \frac{\rho L V^2}{\sigma}$$

where ρ, V, L and σ denote density, velocity, a characteristic length scale, and surface tension of the jet respectively. Experimental observations give a critical Weber number of the order of 10 to 20. Some early experiments by R.O. Ivins[7] using low melting point metals (tin, lead, bismuth, and mercury) seem to support this range of critical Weber numbers between 10 and 20 (Figure 7). The break up of a transient hydrodynamic jet is very sensitive to initial and boundary conditions. For a hot jet, thermal effects may overshadow the hydrodynamic effect. Although the use of a critical Weber number does give a representative criterion for the hydrodynamic mechanism, the presence of other mechanisms renders the Weber number criterion of limited use in practice.

Using the laser fluorescence technique, the structure of an axisymmetirc liquid jet in the plane passing through the axis of symmetry is shown in Figure 8. The bright fluid is the jet fluid and the fragmentation of the jet fluid into a distribution of different sizes is illustrated. The Kelvin-Helmholtz instability mechanism which causes the jet interface to become unstable initially, prior to fragmentation, can also be seen in Figure 8. Using a similar technique, the penetration of a liquid jet into a stationary liquid is shown in Figure 9. Of particular interest to note is the formation of a vortex at the head of the penetrating jet. Similar results are obtained from the computer simulation of a the penetration of liquid jet into another fluid shown in Figure 10. Again the formation of a large vortex at the head of the penetrating jet entraining fluid into the vortex (as observed experimentally) is also obtained. When the coolant is entrained into the vortex and subsequently gets superheated, nucleation and vapor explosion occur in the

vortex. Thus thermal energy is converted into mechanical energy for fragmentation and subsequent dispersion of the fuel jet. The entrainment of coolant fluid into the vortices of the penetrating jet which subsequently gives rise to local vapor explosions causing fragments to form and get dispersed is an important mechanism whereby thermal energy is converted to mechanical energy needed for the break up and dispersion of the fuel fragments. A slight modification of the entrainment mechanism is the so-called entrapment mechanism. In the entrapment mechanism, the fuel jet may trap a certain volume of coolant as it descends to the bottom of the tank. The trapped volume of coolant may subsequently get superheated to the spontaneous nucleation temperature. Explosive formation of vapor follows and the vapor explosion provides the mechanical work for fragmentation and dispersion. The entrapment and entrainment mechanisms differ only in the way in which the coolant fluid gets enclosed by the hot fuel jet. Kelvin-Helmholtz instability (if there is a relative velocity or shear at the interface of the two liquids) and the Taylor instability (when there is a density difference of the fluids across the interface normal to the direction of acceleration) provide the mechanisms for surface perturbations to grow. Surface tension of the liquid fuel prevents the jet break up due to dynamic inertia forces (the Weber number criteria). However, if entrained or entrapped coolant undergoes local vapor explosion, the high local acceleration experienced by the fuel jet due to the expansion of the vapor provides the high inertia forces necessary to overcome the surface tension forces for fragmentation. The trapping of coolant between the descending fuel jet and solid surfaces is strongly dependent on the geometry and initial condition at entry. It is certainly a plausible mechanism.

The Taylor instability is also credited, in a slightly different way, for fragmentation. Consider a volume of fuel in the coolant with a stable vapor film surrounding it. The denser liquid above the vapor film gives rise to a Taylor instability of the coolant vapor interface. Subsequent growth of the perturbation results in liquid-liquid contact when the perturbations have penetrated the thickness of the vapor film. Violent local explosions result when the vapor film collapses and the pressure transients of these violent local explosions cause fragmentation of the fuel. Similarily, when the temperature of the interface drops below the Leidenfrost temperature, the vapor film also collapses into periodic liquid-liquid contact. Again violent

local explosions give rise to high pressure transients (much like nucleation bubble collapse) and fragment the fuel.

Although the fragmentation process is perhaps the most important process of a large scale vapor explosion, it is extremely difficult to predict quantitatively. The major mechanisms that give rise to fragmentation are identified and described, but even on an individual basis, quantitative predictions are very difficult. Combined or mutual cooperative effects of all these various instability mechanisms are not understood even qualitatively. The notorious irreproductibility of vapor explosion experiments is to a large degree due to the sensitivity of the fragmentation mechanisms to initial and boundary conditions. Thus even if the fragmentation processes are described theoretically, it is extremely difficult to ascribe the proper initial and boundary conditions necessary for the comparison between theory and experiments.

6. DISPERSION

Following fragmentation, the fuel fragments themselves must be dispersed rapidly into the volume of coolant. Like the fragmentation process, dispersing the fragments requires energy which must be obtained either from the potential energy of the fuel if initially poured from a height or ejected from a pressure-retaining component into the coolant, or derived from the thermal energy stored in the molten fuel itself. It is of interest to estimate the energy requirement if a volume "V_F" of fuel were to be fragmented into N fragments of average radius "r" and were to get dispersed to form a volume of mixture of fuel fragments of coolant of dimension L in a time u/L (where u is the average speed of the fragments). To form a liquid droplet of radius r, the surface energy required is $4\pi r^2\sigma$ and the kinetic energy for a droplet moving at velocity "u" is $(4\pi r^3/3)\rho u^2/2$. For a droplet moving in a viscous fluid, the work done against viscous drag forces over a distance L is $(\pi r^2)LC_D\rho u^2/2$ where C_D is the drag coefficient. In general, the surface and kinetic energy terms are small compared to the work done against viscous forces during dispersion. Thus the energy for dispersion may be approximated by just the dissipation term $N(\pi r^2)L\rho u^2/2C_D$, and noting that $V_F = N4\pi r^3/3$, the energy for dispersion can be written as

$$E_D = \frac{3}{8} V_F \frac{\rho u^2 C_D L}{r} = \frac{3}{8} \frac{C_D \rho V_F^2}{t_D^2 r}$$

where $t_D = L/u$ is the dispersion time and we have assumed that the size L of the volume of mixture is of the same order as the volume of the fuel $V_F^{1/3}$ (i.e., $L^3 \simeq V_F$). The result of this approximate analysis of a one-step fragmentation and dispersion tells us that the energy requirement is very high if the dispersion time "t_D" and fragment size "r" is small and the volume of fuel to be dispersed "V_F" is very large.

If the fragmentation and dispersion were to proceed in stages according to a geometric progression, i.e., $V_F \longrightarrow V_F/2 \longrightarrow V_F/4$, etc., and by assuming the mixing velocity "u" to be the same for each stage, the dispersion energy is found to be

$$E_D' = 1.8\, L\rho V_F C_D \left(\frac{V_F^{2/3}}{t_D^2}\right) \left(1 - \frac{r^2}{V_F^{2/3}}\right) \ln \left(\frac{V_F^{1/3}}{r}\right)$$

E_D' is much less than E_D for the same "V_F" and final fragments for the same "V_F" and final fragment size 'r'. The ratio E_D'/E_D is obtained as

$$\frac{E_D'}{E_D} = \frac{r}{V_F^{1/3}} \ln \left(\frac{V_F^{1/3}}{r}\right)$$

From the above considerations, it seems highly improbable to obtain the situation of very fine fragments (~100 μm size) rapidly dispersing to form a volume of mixture of coolant and fine fragments for a "volumetric" vapor explosion. A much more probable event is the fragmentation and dispersion of relativley large fragments. Then explosion is initiated locally in the volume of mixture of coarse fragments and coolant. There will also be a propagation phase in which the explosion wave spreads from the local region throughout the volume of mixture itself.

7. PROPAGATION

Consider a volume of a mixture of coarse hot fuel fragments and coolant. The loss of thermal energy of the fragments during dispersion is small due to the presence of a stable vapor film surrounding the fragments. To trigger a vapor explosion locally, further breaking up of the coarse fragments into finer

particles (~100 μm) is necessary for a rapid heat transfer rate and explosive vaporization of the coolant. The various mechanisms of fragmentation discussed earlier for the entire volume of fuel apply also to the case of a single coarse fragment. The most probable trigger event is the collapse of the vapor film due to the Taylor instability, the Markstein or Richtmeyer-Meskhov instability (when the liquid vapor interface is accelerated by a pressure wave), or simply when the temperature of the interface drops below the Leidenfrost temperature bringing about the onset of violent unstable film boiling. Nelson and Duda[8] have demonstrated the onset of vapor explosion when a shock wave is used to collapse the vapor film. With the collapse of the vapor film, liquid-liquid contact occurs, the coarse fragments will disintegrate into finer fragments and a local vapor exlosion results. The expansion of the vapor then drives a shock wave into the volume of mixture. The shock wave produces a number of physical effects all of which contribute towards the break up of other coarse fragments in the path of the shock wave. The shock wave can provide the acceleration necessary for the vapor film collapse due to the Markstein instability. The vapor film may be stripped from the fragments as the shock wave propagates by (the boundary layer stripping mechanism). Also, the impulse imparted by a sufficiently strong shock wave can lead to direct disintegration of the fragment itself. Thus the shock from a local explosion initiates more explosions as it propagates outwards. The subsequent explosions triggered by the shock then provide the energy necessary to maintain the shock from decaying. If coupling between the shock and the explosions that it triggers in its wake occurs, then the situation is analogous to a chemical detonation wave. In a chemical explosive, the shock wave from the initiation source adiabatically compresses and heats the explosive in its wake leading to autoignition. The energy released by the chemical reaction maintains the shock propagation preventing its decay. Such a steady state complex of shock followed by an energy release zone is the classical structure of a chemical detonation wave. Board and Hall[9] have proposed a theory of thermal detonation for the propagation front of the "vaporization wave" through the coarse fragment-coolant mixture. Making a direct analogy to a chemical explosive, they even invoke the Chapman-Jouquet criterion (i.e., sonic flow of the products relative to the wave) for the determination of the speed of the shock-vaporization wave through the fuel-coolant mixture. Board and Hall's

thermal detonation model for the propagation of the wave through the mixture is plausible since the shock does provide the necessary mechanisms for triggering vapor explosions of other fuel fragments it interacts with. However, it is doubtful that the steady state Chapman-Jouquet detonation theory can be applied to vapor explosions in view of the scale required for unconfined detonations.

It should be pointed out that apart from the shock mechanism turbulence and the convective flow disturbances generated by a local explosion can provide equally effective mechanisms for triggering explosions in other fragments, resulting in a propagation of explosion from the triggered site. A shock wave may be a sufficient but also a necessary mechanism. Hence the use of Hugoniot curve and Rayleigh line analysis may be carrying the analogy too far. There are other mechanisms proposed in the literature for the propagation of the vapor explosion wave from a triggered site. All these mechanisms are but slight variations of those discussed previously for fragmentation of the original fuel mass. It is clear that for the propagation of a strong vapor explosion wave, the coarse fragments must disintegrate into very small particles to provide the required heat transfer rate to the coolant. A multitude of physical mechanisms are present for this purpose. However, which one plays the dominant role depends strongly on initial and boundary conditions. It is doubtful that a single universal theory based on one (or more) of these mechanisms is capable of describing the complex phenomenon.

8. SUMMARY REMARKS ON THE MECHANISMS OF VAPOR EXPLOSION

It is clear that for a large scale vapor explosion, a large volume of coolant must be superheated. The superheating comes from the mixing of the coolant with the hot molten fuel. To obtain a high heat transfer rate, large contact surface area is required. This necessitates the fragmentation and dispersion of the hot fuel mass into the coolant volume. Energy considerations suggest that it is more probable to break up the fuel mass into coarse fragments first and then disperse them in the coolant. This is followed by triggering of a local vapor explosion in one or more coarse fragments. The shock wave (or turbulence) from the local explosion then triggers other coarse fragments to explode and a propagation of the vapor explosion results. There are a number of possible fragmentation mechanisms for breaking up the original fuel mass and subsequently the coarse fragments

in a propagating explosion wave. What mechanisms dominate depends on initial and boundary conditions. To prevent excessive heat loss during fragmentation, dispersion and mixing, the contact interface temperature of the fuel must exceed the Leidenfrost temperature so that stable vapor film boiling can be achieved. It is also possible to have a so-called stratified system in which the molten fuel can spread forming a layer with a large surface area in the coolant. Stable vapor film boiling is required to prevent excessive heat loss during the spreading of the molten fuel. Again, local breakdown of the vapor film triggers an explosion which subsequently propagates outwards with shock waves and turbulence providing the mechanisms of collapsing the vapor film as the explosion wave propagates. In the stratified system the fuel mass need not fragment, since the large surface area is achieved by the spreading of the fuel in the coolant. The sequence of events described above is best summarized by the schematic diagram shown in Figure 11.

In conclusion, the following remarks can be stated:

1. Knowledge to predict the quantitative outcome of a given molten-fuel coolant interaction is insufficient.
2. Basic mechanisms have been identified, but cooperative effects due to mutual interaction are not well understood.
3. Key experiments to establish propagation mechanisms,a necessary process for large scale explosion, are required.
4. Each specific reactor design and each specified accident scenario should be analyzed to achieve a meaningful estimate of the outcome. Generalizations are not possible.

II. COMMENT ON THE "ANALYSIS OF THERMAL INTERACTIONS FOLLOWING THE HYPOTHETICAL SEVERE BLOCKAGE OF A COOLANT CHANNEL" BY HENRY AND FAUSKE.[1]

1. INTRODUCTION

This section of the report is devoted to a critical review of the model proposed by Henry and Fauske[10] for the analysis of the thermal interactions following a hypothetical severe blockage of a fuel channel. The major sequence of events assumed in Henry's model is i) a severe flow blockage leading to ii) melting of the fuel bundles, iii) thermal attack on the pressure/calandria tube wall iv) resulting in rupture of the tube v) discharging hot molten fuel and hydrogen mixture from the ruptured tube to the coolant-moderator and vi) the rapid heat transfer from the molten fuel fragments to the coolant-moderator vii) which gives rise to rapid steam generation and vii) the expansion of the vapor bubble, resulting in strong hydrodynamic transients in the calandria vessel. The present report emphasizes this particular aspect of Henry's analysis. The analyses dealing with structural damage associated with jet impingement and ablation by the discharging fuel and hydrogen mixture are not considered here. At any rate, damages associated with jet impingement and ablation are found to be insignificant when compared to that due to the hydrodynamic transients from the expanding steam bubble.

The analysis of Henry and Fauske[10] can be divided into four parts:

1) Modelling of the basic phenomena which includes a) release models for the molten fuel-hydrogen discharge, b) the subsequent steam generation due to heat transfer from the fuel fragments to the moderator, and c) the dynamics of the vapor bubble.
2) Incorporation of the model into a numerical computer program.
3) Testing of the model against experimental results, mainly those from Isles[11].

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[1] **NOTE** All quotations, nomenclature, equations and figures used in this section are extracted and written directly and exactly as they appear in the Henry and Fauske[10] report.

4) The application of the model to the CANDU reactor system for 9 representative cases. Most of the attention in this report is devoted to Part 1 which pertains to the modelling of the basic phenomena.

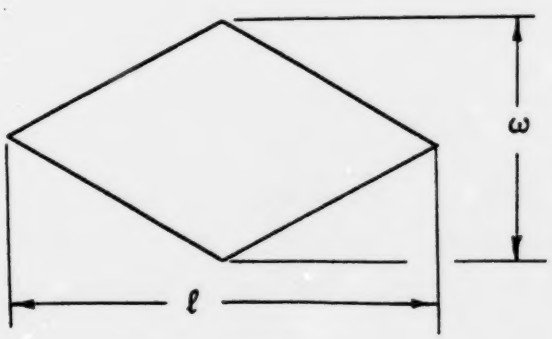
Henry and Fauske[10] proposed two models for the molten fuel discharge: i) a stratified flow model and ii) a dispersed flow model. We shall consider the stratified flow model in Section 2 and the dispersed flow model in Section 3.

2. STRATIFIED FLOW MODEL FOR THE MOLTEN FUEL-HYDROGEN DISCHARGE

2.1 First Stage $0 < t < t_0$

The major assumptions in the stratified flow model are: i) the gaseous hydrogen and the molten UO_2 are separated with the gaseous H_2 occupying the upper half and the molten UO_2 occupying the lower half of the presumably balooned pressure tube; ii) momentum transfer between the phases is considered negligible. The sequence of events in the stratified model is illustrated in Figure 12. For time $t < 0$, the fuel bundles and pressure/calandria tubes are intact. Complete blockage of the fuel channel occurs and results in melting of the fuel to form a molten mass of fuel on the bottom and hydrogen gas on the top inside the pressure tube (Figure 12b). Thermal attack results in the rupture of both the pressure and calandria tubes at $t = 0$ and subsequent discharge of first the molten fuel and then the mixture of fuel and hydrogen gas through the break for $t > 0$.

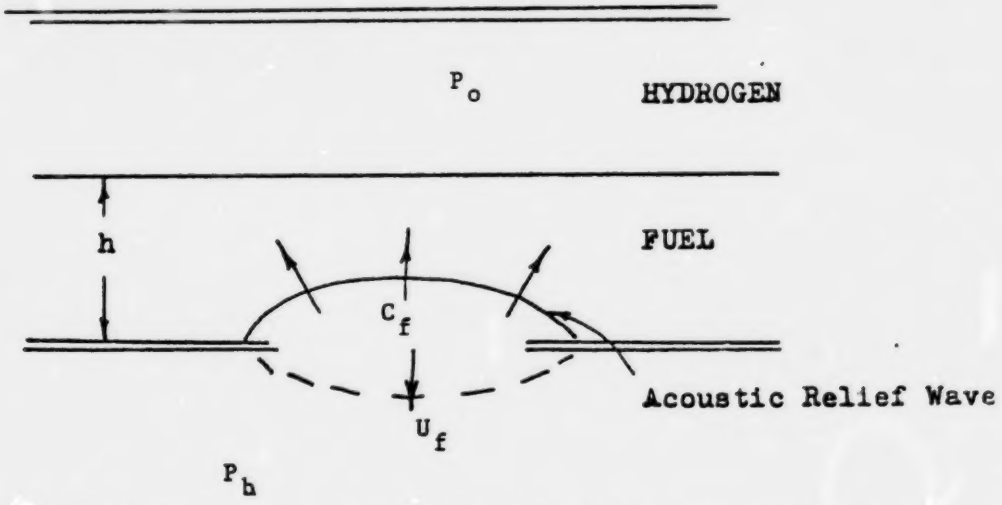
The discharge of the molten fuel and hydrogen is divided into three stages in the stratified model. For Stage 1, $0 < t < t_0$, the rupture starts at $t = 0$ and terminates at $t = t_0$ when the rupture opening has been developed to its maximum size. It is assumed that the shape of the opening is diamond or "fish-mouth" shaped as shown below.



The area "A" of the opening is thus given by $A = \frac{1}{2}\omega\ell$.

It is further assumed that the final maximum area "A_M" of the opening is twice the internal flow area of the pressure tube "A_0". Thus, $A_M = 2A_0 = \omega_0\ell_0/2$, or the final width of the opening $\omega_0 = 4A_0/\ell_0$. The time "t_0" to open to the maximum area "A_M" is chosen as a parameter between 0.5 ms to 5 ms in the computation. This range for "t_0" is said to be based on experimental observations. During the opening stage $0 < t < t_0$, it is assumed that the growth rates for the length and width of the opening are linear, i.e., $\omega(t) = \omega_0(t/t_0)$.

When the tube begins to rupture at $t = 0$, an acoustic relief wave will propagate from the rupture opening back into the molten fuel layer. Behind the acoustic relief wave, the fuel will be accelerated and its velocity increases by $\Delta U = \Delta P/\rho C$, as given by the acoustic solution.



The acoustic relief time, $t_a = 2h/C_F$, and the fuel at the exit receives an increment in velocity in time units of $2h/C_F$. Thus during the acceleration stage of the fuel out of the ruptured channel, the fuel discharge velocity is given by:

$$U_F = \frac{P_0 - P_b}{\rho_F C_F} [1 + 2\mathrm{INT}(t/t_a)] \quad ,$$

where the function INT(X) is introduced to truncate a real number X to the largest integer, i.e., INT(1.2) = 1, INT(1.9) = 1, INT(2.1) = 2, etc. After a number of units of t_a, steady state will be reached and the steady state

discharge of the liquid metal is given by Bernoulli's equation as:

$$U_F = \sqrt{\frac{2(P_0 - P_b)}{\rho_F}}$$

Thus the procedure for calculating the fuel discharge during Stage I is as follows: 1) Calculate the acoustic relief time $t_a = 2h/C_F$. Calculate the fuel velocity due to acoustic acceleration, i.e.,

$$U_F = \frac{P_0 - P_b}{\rho_F C_F} [1 + 2\text{INT}(t/t_a)] \quad .$$

From the pressure difference next compute the steady state velocity using Bernoulli's equation, and take the smaller of the two. Next calculate the instantaneous ruptured area.

$$A(t) = \frac{1}{2} (\frac{\omega_0}{t_0})(\frac{l_0}{t_0}) t^2 \quad ,$$

and the molten fuel discharge rate is then given by

$$\dot{W}_F = \rho_F U_{F0} A(t)$$

where U_{F0} is the smaller of the two values U_F and U_{FS}. The above is in essence the procedure developed to calculate the fuel discharge rate during the first stage of the rupture $0 < t < t_0$.

It is observed that there exists an inconsistency in the model for this first stage of the discharge. It is assumed that the duration of stage I is when the rupture area goes to its maximum value of $2A_0$, and the time to achieve this is t_0. In the analysis t_0 is taken to be a parameter with arbitrary values ranging from 0.5ms to 5ms. However, in Henry's analysis (pp2-5)[10] it is stated that "The duration of the first stage is determined by the time required to discharge the continuous molten fuel layer above the break and allow communication between the high pressure hydrogen zone and the expanding steam region ...". Thus the duration of stage I is governed simultaneously by two requirements: i) the rupture area to reach its maximum value of $2A_0$ and ii) depletion of the molten fuel layer, permitting the high pressure hydrogen above the molten fuel layer to be discharged into the bubble. Depletion of the fuel layer depends on the thickness of the layer "h" and the discharge rate $\dot{W}_F$, and hence t_0 cannot be taken arbitrarily to be in between 0.5 ms and 5 ms.

2.2 Second Stage $t_0 < t < t_1$

The termination of Stage 1 is when $t = t_0$, the fuel layer is depleted at the rupture site, hydrogen starts to be discharged and depressurization occurs. Also the propagation of the break stops and the break area $A \longrightarrow 2A_0 = \omega_0 \ell_0/2$. Stage 2 begins at $t = t_0$ and terminates when $t = t_1$ where t_1 is taken to be the time when the rarefaction wave has reached the ends of the fuel channel. If the break occurs at the middle of the fuel channel of length L, then $t_1 = L/2C_{TP}$ where C_{TP} is the sound speed of the two-phase stratified mixture taken as

$$C_{TP} = C_g \sqrt{1 + \left(\frac{1-\alpha}{\alpha}\right)\left(\frac{\rho_g}{\rho_l}\right)}$$

where α is the void fraction, ρ_g, ρ_l denote the gas and liquid densities, respectively. Since $\rho_l >> \rho_g$, we note that $C_{TP} \approx C_g$ the discharge flow for Stage 2 in the stratified model as shown in Figure 13.

In the calculation of the fuel discharge rate, two expressions were again employed to evaluate the flow velocity out of the break, the steady flow velocity U_{FS} using Bernoulli's equation, i.e.,

$$U_{FS} = \sqrt{\frac{2(P_0 - P_b)}{\rho_F}} ,$$

and the velocity increment induced by the acoustic pulse U_F, i.e.,

$$U_F = \frac{P_0 - P_b}{\rho_F C_F} .$$

Again the smaller of the two values is chosen and denoted by U_{FI}. With U_{FI} determined, the molten fuel discharge rate $\dot{W}_F$ is evaluated by the expression

$$\dot{W}_F = 2\rho_F U_{FI} A_F$$

where A_F is the portion of the cross-sectional area of the pressure tube occupied by the molten fuel. For the discharge rate of the hydrogen gas (or D_2), the discharge velocity is assumed to be that due to acoustic pulse acceleration, i.e.,

$$U_h = \frac{P_0 - P_b}{\rho_h C_h} ,$$

where ρ_h is the density of hydrogen at pressure P_0, and temperature T_0, C_h is the sound speed of hydrogen and U_h is upper bounded by C_h, i.e., $U_h \leq C_H$. P_b

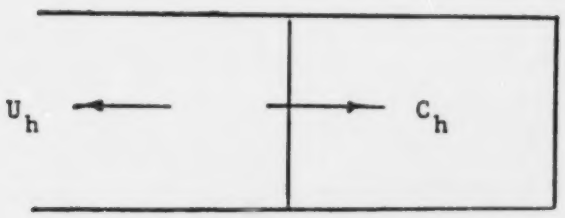
is the pressure in the steam bubble. Due to the fact that the discharge flow is "choked" at the break, the pressure across the acoustic pulse will not be $P_0 - P_b$, instead it is $P_0 - P_1$, and since the flow is sonic $U_h = C_1$ where C_1 is the sound speed at the break where the Mach number is unity, i.e., $M = 1$. It is estimated that $P_1 \simeq 0.1P_0$ and, since the flow out is normal to the direction of the flow accelerated by the acoustic pulse, Henry and Fauske[10] simply take $P_1 = 0.5P_0$ arbitrarily to take care of the change in direction.

It can be noted that since Stage 2 is from $t_0 < t < t_1$, the discharge flows of fuel and hydrogen are never steady state. Thus, the use of Bernoulli's equation for the fuel discharge velocity is not valid. It is not clear what C_F denotes in Henry's formula for the flow velocity of fuel behind the acoustic pulse. Is $C_F = C_{TP} \simeq C_h$? Perhaps the major comments on the analysis of Stage 2 are related to: i) the pressure difference across the acoustic pulse and ii) the arbitrariness in taking the change in direction of the flow of fuel (and hydrogen) from parallel to normal direction to the axis of the fuel channel. It is not clear as to how the discharge rate for Stage 2 is evaluated. Furthermore there are no convincing arguments for the arbitrary assumptions made in the analysis.

2.3 Stage 3, $t > t_1$

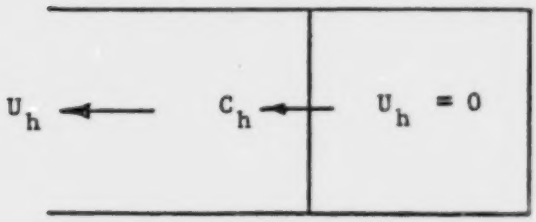
Stage 2 terminates when $t = t_1$ and the rarefaction waves arrive at the ends of the tube. Stage 3 is defined by $t > t_1$. Since one end is assumed completely blocked (right side of the break) and the other end open (left side), separate considerations are made for the two sides.

For the blocked end the rarefaction wave will be reflected back as an expansion wave. While the incident rarefaction wave accelerates material to the left as sketched below.



incident wave

the reflected wave will stop the flow behind it, i.e.,



reflected wave

In Henry's report (pp. 2-10)[10] it is stated that "as a result, molten fuel would cease to be discharged out of the pressure/calandria tube from the blocked half of the power channel. This state would persist until the second wave propagates back to the rupture site ... ". In our opinion this is incorrect. The reason is that flow will continue to be discharged out of the break and stop only when the reflected wave arrives at the break itself, not when the reflected wave just starts from the blocked end upon reflection of the incident wave. Also depending on the interaction pressure when the reflected wave reaches the break, a third rarefaction wave may be formed propagating back towards the blocked end again. In this case the flow out of the break will continue.

For the unblocked end, the incident rarefaction wave will be reflected as a compression wave. It is assumed that the reflected compression wave propagates back at the acoustic speed of the hydrogen i.e., C_h. Flow induced by this compression pulse is given by

$$U_{h,INC} = \frac{P_f - P_b}{\rho_h C_h}\left[1 + 2INT(t/t_a)\right]$$

where $t_a = 2L_h/C_h$ is the acoustic relief time and the acoustic relief length L_h is defined by $L_h = L_2 - L_f$. A sketch showing the discharge in Stage 3 from the open end is shown in Figure 14. Henry and Fauske[10] assume flashing of the liquid behind the wave into the volume V_f (the bubbly volume shown in Figure 14) V_f is thus bounded by the reflected compression pulse that propagates back to the break and the expansion wave that travels upstream to the outlet header

at the acoustic speed C. Given V_f, P_f, then the quality of the bubbly mixture X_f can be obtained by assuming equilibrium behind the wave. Again the outflow from the break will not be influenced until the reflected compression wave actually returns to the break. Hence, calculations using Eq. 2.17 of Henry's report[10] are not correct.

Although not explicitly stated in Henry's report, it is presumed that the total outflow during Stage 3 is taken to be the arithmetic sum from each side of the tube.

Major criticisms of the analysis for Stage 3 include:

1. The important error is assuming that the discharge is affected when the incident waves arrive at the ends of the tube. In reality, the flow at the break is influenced only when the reflected waves return to the break itself.
2. The flow from the left and right sides of the break cannot be decoupled, particularly in computing the condition when the reflected waves from the ends meet at the break.
3. As in Stage 2, the comment regarding the discharge flow being normal to the flow induced by the axial acoustic waves applies here also.
4. It is not clear how the boundary conditions behind the reflected wave from the open end are determined.

3. THE DISPERSED FLOW MODEL

While the stratified model assumes the molten fuel and hydrogen gas to be separated, the dispersed model assumes a complete homogeneous mixing of the two-phase medium behind the first rarefaction wave. For the sound speed in a dispersed medium, the following expression is used:

$$C_{TP} = C_g \sqrt{\left[\alpha^2 + \frac{\alpha(1 - \alpha)\rho_\ell}{\rho_g}\right]^{-1}}$$

3.1 Stage 1, $t < t_o$

The computation for the discharge flow is again divided into three stages with the first stage identical to that considered previously for the stratified model.

3.2 Stage 2, $t_0 < t < t_1$

Stage 1 terminates when the break opening reaches its maximum value, i.e., $A \longrightarrow 2A_0$ and also the molten fuel layer, depleted with the hydrogen gas, has started to be discharged through the break. The depressurization now causes a rarefaction to be propagated back towards the ends of the fuel channel from the break. When these rarefaction waves arrive at the ends, $t = t_1 = L/2C_{TP}$ (where it was assumed the break to be centrally located) and Stage 2 terminates. Behind these rarefaction waves the fuel and hydrogen are assumed to form a homogeneous mixture as illustrated in Figure 15.

The discharge flow velocity of the two phase mixture induced by the acoustic pulse is given by:

$$U_{MIX} = \frac{P_0 - P_b}{\rho_M C_{TP}}$$

where $\rho_M = \alpha\rho_h + (1 - \alpha)\rho_F$ is the mixture density and $\alpha = A_0/(A_0 + A_F)$ is the void fraction. Since the induced flow velocity by the acoustic pulse is axial and normal to flow out of the break, it is again assumed that the change in flow direction can be accounted for by taking $P_b = P_1 = 1/2P_0$ when P_b is low and $P_0 - P_b$ otherwise. With the mixture flow velocity obtained, the fuel and hydrogen discharge rate is given by

$$\dot{W}_F = 2\rho_F U_{MIX} A_F$$

$$\dot{W}_H = 2\rho_H U_{MIX} A_0$$

where A_F and A_0 are the cross-sectional areas of the fuel channel occupied by the fuel and hydrogen respectively.

3.3 Stage 3, $t > t_1$

Stage 2 terminates when the rarefaction waves reach the ends of the fuel channel at $t = t_1$, and Stage 3 is defined by the duration $t > t_1$. In this stratified case, a reflected rarefaction is generated at the blocked end (right side of the break) which brings the velocity to zero. However, in the dispersed flow model, it is assumed by Henry and Fauske[10] that the mixture has a substantial momentum towards the break. Thus the reflection is complicated, and to simplify the analysis it is assumed that "the molten material in the blocked end comes to rest after the second stage and remains at rest until the molten material in the unblocked end has been discharged. At

this time the remaining molten material in the blocked end is assumed to be discharged at the fully developed rate of the unblocked end..." This assumption is completely arbitrary and unjustified!

For the unblocked end, the rarefaction is reflected as a compression wave propagating back to the break. Step wise acceleration of the material per acoustic pulse relief time $t_a = 2L_2/C_{TP}$ is given by

$$U_{MIX} = \frac{P_0 - P_b}{\rho_M C_{TP}} \left[1 + 2INT(t/t_a)\right]$$

The pressure P_0 is assumed constant throughout this stage. With U_{MIX} known, the fuel material discharge rate is given by

$$\dot{W}_F = \rho_F U_{MIX} A_F$$

and similarly,

$$\dot{W}_H = \rho_H U_{MIX} A_0$$

for the hydrogen discharge rate.

As in the case of the stratified model, Henry and Fauske[10] assumed that the flow at the break changes when the incident wave arrives at the ends of the tube. In reality, the flow exiting from the break will only be affected when the reflected waves have returned to the break itself. A general comment can be made regarding the entire treatment of the dispersed flow model. Although an attempt is made to develop a sophisticated model to describe realistically the different stages of the discharge process, the over-simplifications and arbitrary assumptions made along the way without valid justifications completely negate the complex model proposed. Therefore, it is not possible to assess whether the results are meaningful from both the stratified and the dispersed flow models for the computation of the fuel and hydrogen discharge rates.

4. STEAM GENERATION

The key mechanism for the generation of the pressure transients in Henry's model[10] is the expansion of the steam and gas bubble at the break. The discharge of the hot molten fuel fragments provides the thermal energy for the generation of steam from the moderator coolant. The bubble expansion rate is governed by the steam generation rate (plus the rate of H_2 gas discharge in Stages 2 and 3). The steam generation rate is, in turn, governed by the rate

of fuel discharge. This is computed differently for the different stages.

For Stage 1, where only fuel is being discharged, the heat added to the bubble is given by

$$\Delta Q_F = \left[\sum_{i=N_0}^{N} \frac{W_{Fi}}{R_{tK} t_{Ki}} (h_F + C_F(T_F - T_b))\right]\Delta t$$

where W_{Fi} is the fuel discharged during the i^{th} time increment.

- R_{tK} - is a relaxation factor
- t_{Ki} - is the time of flight across the bubble
- N - is the current number of time increments
- N_0 - is the earliest time increment
- h_F - the heat of fusion of the fuel
- C_F - the specific heat of the fuel
- T_F - the temperature of the fuel
- T_b - the temperature of steam in the bubble

A schematic diagram illustrating the discharge and bubble growth is shown in Figure 16.

Neglecting the work done, the net energy added to the steam bubble is given by:

$$\Delta Q_F - \Delta Q_c = M_s'(h_s - h_s') + (M_s - M_s')(h_s - h_l)$$

where ΔQ_F = heat added by fuel in Δt.

- ΔQ_c - heat loss by condensation in Δt
  - $h_c A_{CH} (T_b - T_\ell)\Delta t$ where h_c is the heat transfer coefficient
- M_s' - steam in the bubble at time $t - \Delta t$
- M_s - steam in the bubble at time t
- h_s - enthalpy of steam in bubble at t
- h_s' - enthalpy of steam in bubble at $t - \Delta t$.

The amount of steam produced in Δt is given by

$$\Delta M_s = M_s(t) - M_s(t - \Delta t) = M_s - M_s'$$

Dividing the above equations by Δt we get the rate equation

$$\Delta\dot{Q}_F - \Delta\dot{Q}_C = \frac{\Delta M_s(t)}{\Delta t} (h_s - h_s') + \frac{\Delta M_s}{\Delta t} (h_s' - h_l)$$

$$\dot{M}_s(t) = \frac{M_s}{\Delta t} = \frac{(\Delta\dot{Q}_F - \Delta\dot{Q}_c) - M_s(t)(h_s - h_1)/\Delta t}{(h_s' - h_1)}$$

or

$$\dot{M}_s = \frac{(\Delta\dot{Q}_F - \dot{Q}_c) - M_s'(t - \Delta t)(h_s - h_s')/\Delta t}{h_s - h_1}$$

The number of moles of steam in the bubble is given by

$$N_S = N_S' + \dot{M}_s \Delta t/\mu_s$$

where N_s' is the number of moles at $t - \Delta t$,

$\dot{M}_s\Delta t$ - steam generated in Δt

μ_s - molecular mass of steam

For Stage 2, $t_0 < t < t_1$, hydrogen is discharged into the bubble in addition to the fuel fragments. The rate of hydrogen added to the bubble is given by

$$\dot{W}_H = 2\rho_H U_{MIX} A_0$$

and hence, the number of moles of H_2 is

$$N_A = N_H' + \frac{\dot{W}_H \Delta t}{\mu_H}$$

From the energy balance in the bubble

$$\dot{M}_b = \frac{(\Delta\dot{Q}_F - \Delta\dot{Q}_c) - M_b(h_b - h_b')/\Delta t + \dot{W}_H C_{PH}(T_H - T_b)}{h_b - h_1}$$

For Stage 3, $t > t_1$, the hydrogen discharge rate is given by

$$\dot{W}_H = \rho_H U_{MIX} A_0$$

and $\dot{M}_s$ is as computed previously. With the mass of steam plus hydrogen calculated, the pressure in the bubble can be found. The total number of moles of hydrogen and steam in the bubble is $N_H + N_s$. Assuming the bubble temperature T_b to be equal to the saturation temperature at its partial pressure mole fraction

$$X_s = \frac{N_s}{N_H + N_s}$$

the pressure of the bubble is given by

$$P_b = P_T = \frac{ZN_T RT_b}{V_b}$$

where P_T is total pressure (i.e., partial pressure of steam plus hydrogen), N_T the total number of moles, V_b is the volume of the bubble and Z is the

compressibility factor assumed to be unity.

The major comments on the steam generation analysis lies in heat transfer calculations. This will be elaborated further in section 6 of Part II.

5. BUBBLE DYNAMICS

The pressure field generated is associated with the expansion of the spherical bubble. It is assumed that quasi-steady flow exists and shock waves are not considered. For an expanding sphere with velocity U_b in a liquid, the conservation of mass gives

$$\frac{1}{r^2} \frac{\delta}{\delta r} (r^2 u) = 0$$

Thus, $r^2 u$ = constant with $r = R$ ^2R when evaluated at the surface of the bubble $r = R$. Thus the flow velocity at any field point r is $u = (R/r)^2 \dot{R}$ and the momentum equation, i.e.,

$$\frac{\delta u}{\delta t} + u \frac{\delta u}{\delta r} = - \frac{1}{\rho} \frac{\delta P}{\delta r}$$

can be written in terms of R as

$$\ddot{R} \left(\frac{R}{r}\right)^2 + \frac{2\dot{R}^2 R}{r^2} - \frac{2\dot{R}^2 R^4}{r^5} = - \frac{1}{\rho} \frac{\delta P}{\delta r}$$

Integrating the above equation between $r = R$ and $r = r$, the pressure field is given by

$$P(R) - P(r) = -\rho \left[(\ddot{R}R^2 + 2\dot{R}^2 R) \left(\frac{-1}{R} + \frac{1}{r}\right) + \frac{2\dot{R}^2 R^4}{4} \left(\frac{1}{R^4} - \frac{1}{r^4}\right) \right]$$

Neglecting the bubble acceleration under the quasi-steady flow approximation, i.e., $\ddot{R} = 0$ and define $y = R/r$, we write the above equation as

$$P(r) = \left[P(R) - P_\infty\right] \left(\frac{4}{3} y - \frac{y^4}{3}\right) + P_\infty$$

where $P(R) = P_b$ is the bubble pressure .

P_∞ is the pressure far from the bubble, and

$$R = U_b = \sqrt{\frac{2}{3} \frac{P(R) - P(\infty)}{\rho}}$$

is the bubble velocity.

The above equation is used to find the impulse loading on the adjacent calandria tubes. It should be emphasized that the expression is only valid for steady state flow. Shock waves are not treated. For a sudden rupture of

the pressure/calandria tubes with the rapid fuel injection into the moderator coolant (backed by high pressure H_2), the initial steam generation rate is sufficiently high to produce a high bubble velocity. Thus the assumption of quasi-steady bubble expansion in an incompressible fluid must be justified by at least making an order of magnitude type of analysis for shock compressibility effects.

6. HEAT TRANSFER AND FRAGMENTATION (APPENDIX A OF THE HENRY'S REPORT)[10]

In Henry's model, the molten material is assumed to be discharged from the break in the form of fine particles of the order of 100 μm size. The mechanism of fragmentation is assumed to be hydrodynamic, thus governed by a critical Weber number criterion. In Henry's analysis the Weber number is defined by

$$W_e = \frac{\rho_g (U_g - U_F)^2 d_F}{\sigma_F}$$

where ρ_g is the gas density, $(U_g - U_F)$ is the relative velocity, d_F the particle diameter, σ_F is the surface tension. Assuming a critical Weber number of 12 for individual droplet breakup and 120 for a dense array, Henry evaluated the spectrum of particle size to be 37.5 μm to 3750 μm in diameter (typical values chosen are 60m/s < $(U_g - U_F)$ < 600 m/s, σ_F = 0.45 N/m, ρ_g = 4 kg/m^3). Then quoting from experimental observations that the debris size after a vapor explosion is of the order of a few hundred microns, Henry chose a typical size of 100 μm. It should be noted that the debris size after an explosion cannot be taken as representative of the fragment size before the explosion since coarse fragments may undergo further break up in the process of the explosion itself. There appears to be no convincing justification for the choice of fragment size to be 100 μm in Henry's model. This assumption is arbitrary.

Assuming a certain fragment size the next assumption is concerned with the heat transfer rate as the fragments are injected into the steam bubble. Since the contact temperature is of the order of 614°K and is greater than the homogeneous nucleation temperature of 550°K for the moderator, a vapor film will be formed. Since the Biot number is small, one can assume the temperature in the fuel fragment is uniform. (Biot no. = $\bar{h}L/K_F$ where $\bar{h}$ is the averaged surface conductance, L is the volume to area ratio and K_F is the thermal conductivity). As a typical example, for UO_2, $K_F \simeq 3.3 W/m°Ks$.

$L \simeq 33\ \mu m$, $\bar{h} \simeq 20000\ W/m^2{}^oKs$ based on the Board and Hall[9] experiments and the value of the Biot number obtained based on the above values is of the order of 0.2. Thus it was assumed that the particle has a uniform temperature from the surface to the core.

For the quenching of the fuel particle, an energy balance gives the decrease in the internal energy equals to the net heat flux from the particle, i.e.,

$$-C_F\rho_F V_F T_F = \bar{h}A_s(T_F - T_\ell)dt$$

where

- C_F - heat capacity of the fuel
- ρ_F - density of the fuel
- V_F - volume of the particle
- $\bar{h}$ - average heat transfer coefficient
- T_F - fuel temperature
- T_l - coolant temperature.

The solution of the above equation gives:

$$\frac{T_F - T_l}{T_{F0} - T_l} = e^{-\left(\frac{\bar{h}A_s}{C_F\rho_F V_F}\right)t}$$

where the time constant for the cooling is given by

$$\left(\frac{\bar{h}A_s}{C_F\rho_F V_F}\right)$$

Taking typical values for $\bar{h} = 20{,}000\ W/m^2\ {}^oK\ s$, $C_F = 600\ W/kg\ {}^oK$, $\rho_F = 8540\ kg/m^3$, $V/A_s = 33\ \mu m$ the value of $\bar{h}\ A_s/C_F\rho_F V_F = 118$. Thus for the temperature of the particle to drop to one-tenth of the original temperature the time required is of the order of 0.02s (20ms). In other words, the above estimate states that the particle loses 90% of its internal energy in 20ms.

If the mechanism of heat transfer is via conduction in a liquid-liquid contact, then based on Sneider's curves (Figure 17), the value of the parameter

$$F_0 = \frac{\alpha t}{\delta_1^2} \text{ is } 0.3 \text{ for } \frac{T_F - T_l}{T_{F0} - T_l} = 0.1 \ .$$

The diffusivity $\alpha = \frac{K_F}{\rho_F C_F} = \frac{3.3}{8740 \times 600} = 6.44 \times 10^{-7}\ m^2/S$, $\frac{\alpha t}{\delta_1^2} = .3$

gives t = 4.6 ms. Thus the particle loses 90% of its internal energy if the mechanism is via conduction in about 4.6 ms. In Henry's analysis, he chose 20 ms as being more appropriate since film boiling heat transfer is most likely to occur because of the high surface temperature.

Perhaps the most unrealistic assumption in the heat transfer analysis is to assume that the particle loses its latent and sensible heat during the time of flight across the bubble. Assuming a relative velocity $U_g - U_F = 600$ m/s and a maximum bubble radius of 1m, the time of flight is only 1.6 ms. Even for a relative velocity of 60 m/s, the time of flight is 16 ms, which is less than the 20 ms estimated by Henry. Furthermore the steam bubble will be much less than a meter in radius during the initial expansion phase where the major discharge of the molten fuel occurs. Without justification, Henry's model takes the heat transfer time to be the time of flight of the fragments across the bubble (i.e., $t_{Ki} = R_b/U_{Fi}$), and then introduces a correction factor $R_{tK} = 1.2$ to take care of the incomplete heat transfer during the time of flight.

As an example, if the bubble radius is $R_b = 0.5$m, and $U_F = 60$ m/s, then the time of flight $t_K = 0.5/60 = 8.3$ ms. Assuming surface heat transfer, i.e.,

$$\frac{T_F - T_1}{T_{F0} - T_1} = e^{-118t}$$

one gets a value for $\frac{T_F - T_1}{T_{F0} - T_1} \simeq .4$.

Thus only 60% of the energy is lost during the time of flight. If the particle now impacts on the liquid, the use of Sneider's curves for conduction heat transfer gives another 3 ms for removal of 80% of the energy. Thus for this case the removal of 90% of the initial internal energy of the fragment takes 11.3 ms instead of the time of flight of 8.3 ms. In Henry's analysis, the heat transfer is arbitrarily prolonged by multiplying the time of flight by a factor $R_{tK} > 1$.

An important assumption in Henry's model is to exclude the possibility of large fragments. With $T_I > T_{HN}$ of about 550°K, stable film boiling is possible and the coarse fragments retain their internal energy sufficiently long to form the mixture of coarse fragment and coolant. When triggered, this volume of mixture suddenly undergoes phase change to vapor and a violent vapor explosion

results accompanied by shock waves. The assumption by Henry that the particles come out fine (~100 μm) negates the formation of violent steam explosion. The only possible hydrodynamic transients are due to the steady expansion of the bubble whose expansion rate depends on the heat transfer rate, which in turn depends on the mass of fuel discharged. As pointed out previously there are a lot of assumptions made in the heat transfer analysis that are not justified. Most important is to link the time of flight to the energy loss of the particle.

7. COMMENTS REGARDING APPENDICES B, C AND D[10]

There are a few comments on Appendices B, C and D pertinent to the model[10]. These are briefly stated in the Section here prior to returning to discussion of the results predicted by the model itself.

In Appendix B, in the discussion of the sound speed in a two-phase mixture, the most serious assumption made is that the wave is assumed to be planar. This is difficult to justify particularly for the stratified case where the difference in sound speed between the gas and the molten fuel is very large.

In Appendix C, on the acoustic acceleration of the molten fuel, the formula used for the velocity increment across the wave is

$$\Delta U_F = \frac{P_0 - P_b}{\rho_F C_F}$$

This is only valid for weak acoustic pulses, i.e., when $P_0 - P_b$ is small. In the present case, $P_0 - P_b$ is not small, and the expansion wave is a continuous wave train. Finite amplitude wave analysis must be used to find the velocity of the material accelerated by a rarefaction fan.

In Stage 1 of the discharge where only fuel is ejected, the expansion waves emitted at the break diverge as they propagate into the fuel layer. The fuel flow from the layer through the break is three dimensional. The use of a planar wave one dimensional analysis must be justified.

In Appendix D, where the pressure tube failure mode is discussed, the experimental observation of Baum is used to give a crack propagation velocity of 200 m/s. Thus for a fish-mouth break of a tube 20 cm diameter, the time for the break area to be twice the cross sectional area of the tube can be found to

be 0.5×10^{-3}s. In the base case analysis where a crack length of 0.42m is used, the time of opening will be 1 ms. If one assumes arbitrary crack length, then the maximum area being twice the cross-sectional area of the tube for the termination of Stage 1 cannot be met. The break area is dependent on the assumed crack length. For example, in later calculations when the crack length of 2m is used, the break area will be 5 times the cross-sectional area of the tube. Thus the requirement of a break area being twice the cross-sectional area is inconsistent with the independent assumption of an arbitrary crack length. Also if the crack propagation velocity is taken to be 200 m/s, the opening time t_0 cannot be taken arbitrarily. If t_0 is assumed arbitrary as in the analysis, then the crack propagation speed is arbitrary. It seems that the assumptions for Stage 1 that i) $A_r = 2A_0$, ii) an arbitrary crack length and iii) an arbitrary opening time t_0, and iv) blow out of the fuel layer, are inconsistent. The time to develop a rupture area of $2A_0$ is governed by crack propagation dynamics. The time to "blow out" the molten fuel layer is governed by the fluid mechanics of the transient liquid flow. Once blow out occurs, and with rapid depressurization of the hydrogen, the crack stops propagating. Thus the crack length cannot be arbitrarily assumed.

8. COMMENTS ON SECTION 3, COMPARISON WITH ISLES EXPERIMENTS[10]

The model developed by Henry[10] was used to compare with Isles experiments[11] and it is stated that:

> "The close agreement of the numerical predictions with the respective experimental results shows that this numerical model provides a good representation of the integrated hydrodynamic transient behaviour following a pressure tube melt-through accident. With this confidence the basic model can be applied to hypothetical accident conditions in the CANDU reactor system, using input data pertinent to the particular characteristics of the geometric configurations and thermodynamic conditions of the reactor system."

The above statement is an over-estimation of the capability of the simple model developed and an underestimation of the complexity of the vapor explosion phenomenon. It is well-known that all molten fuel-coolant interaction experiments are highly irreproducible. It is difficult to draw definitive conclusions from the Isles experiments alone. Hence, to compare a theory with

one set of limited experimental data as proof of validity of the model, and to gain confidence from this comparison to apply to other situations, is inappropriate. Also, not the entire model is used, only Stage 1 was considered. The duration for Stage 1 varies from $1ms < t_0 < 5ms$. It is not clear what parameters are used in the comparison between theory and experiment in Figure 3.9 of the report[10]. (Figure 3.9 is reproduced here as Figure 18, for ready reference.) In the comparison with experiments, it is stated that the more rapid rise time and higher peak pressures observed are due to the physical interaction of the pressure transducers with the debris or inaccurate calibration. If the Authors doubted the validity of the experimental results, it seems inconsistent that they should later conclude that good agreement with the experimental results lends support to their theory and gives them confidence.

The so-called 'good agreement' with experimental results is based on scaling down the experimental values by a factor of two. Also the time scale of the pressure transients is much smaller than can be influenced by the deformation of the tank wall. Hence this argument cannot be used to justify why their "infinite sea model" gave results in better agreement with experiments.

9. COMMENTS ON SECTION 4, APPLICATION TO THE REACTOR SYSTEM[10]

After validating the theoretical model by comparison with the experimental observations of Isles, nine test cases were completed. In order to best assess the value of the results obtained, the calculation procedures are summarized below with specific comments made along the way to point out the doubts and inconsistencies.

For Stage 1, $0 < t < t_0$, t_0 is assumed arbitrarily as a parameter. The discharge rate of fuel is given by

$$\dot{W}_F = \rho_F U_{F_0} A_r$$

$$A_r = (\frac{1}{2})(\frac{W}{t_0})(\frac{\ell}{t_0})t^2$$

$$W = \frac{4A_0}{\ell}$$

when $t = t_0$, $A_r = 2A_0$. If this is a requirement for t_0, then it is clear that

the crack length cannot arbitrarily be assumed as in Table 1. If the $A_r = 2A_0$ condition is relaxed then t_0 still cannot be assumed arbitrarily if the crack propagation velocity of 200 m/s is also relaxed. To find the discharge velocity, both the acoustic and the steady state (Bernoulli's Equation) equations are used, i.e.,

$$U_{F0} = \frac{P_0 - P_b}{\rho_F C_F} \left(1 + 2\text{INT}(t/t_a)\right)$$

$$t_a = \frac{2h}{C_F}$$

$$U_{FS} = \sqrt{\frac{2(P_0 - P_b)}{\rho_F}}$$

and the smaller of the two U_{F0} or U_{FS} is used.

The acoustic formula is for weak acoustic pulses when the amplitude is small, i.e., $P_0 - P_b$ is small. Otherwise finite amplitude wave analysis must be used. Initially for Stage 1, $P_0 - P_b$ is large. Hence, the acoustic formula is not valid. In evaluating t_a, the thickness of the fuel layer was not given in the calculations. Also, since the entire process for Stage 1 is transient, U_{F0} should always be less than the steady flow equation for U_{FS}.

Regarding steam generation rate $\dot{M}_S$, the following equation is used:

$$\dot{M}_s = \frac{\Delta\dot{Q}_F - \Delta\dot{Q}_C - \frac{M_s'}{\Delta t}(h_s - h_s')}{(h_s - h_f)}$$

with

$$\Delta\dot{Q}_F = \sum_{i=N_0}^{N} \frac{W_{Fi}}{R_{tK}\, t_{Ki}} \left(h_f + C_F(T_F - T_b)\right)$$

$$W_{Fi} = \sum \dot{W}_{Fi}\, t_{ki}$$

To account for incomplete heat transfer during the time of flight, an arbitrary value for R_{tk} was chosen (i.e., $R_{tk} = 1$ or 1.2). The value for $\Delta\dot{Q}_c$ is unknown and has to be assumed . $(h_s - h_s')$ is based on the steam temperature which is unknown. Assuming $\dot{M}_S$ is calculated, then

$$M_S(t) = \sum \dot{M}_S\, t_{Ki}$$

and the number of moles of steam in the bubble is given by

$$N_S = N_S' + \frac{\dot{M}_S\, t_{Ki}}{\mu_S}$$

To calculate the pressure in the bubble, the equation of state is used.

$$P_b = \frac{ZN_T R_g T_b}{V_T}$$

with the compressibility factor $Z = 1$, and $N_T = N_S + N_H$ which for Stage 1, no hydrogen is emitted, thus $N_H = 0$. The bubble radius is $R = \dot{R} t_{K1}$, and the volume $V_T = \frac{4\pi}{3} R^3$. The bubble velocity is

$$U_b = \dot{R} = \sqrt{\frac{2}{3} \frac{P_b - P_\infty}{\rho_\ell}}$$

If P_b is known, R is known, then the pressure field is given by

$$P(r) = [P(R) - P(\infty)] \left(\frac{4}{3} y - \frac{y^4}{3}\right) + P_\infty$$

where $y = R/r$. The calculations proceed until $t = t_0$ where t_0 is fixed a priori. If there is a delay in heat transfer from the fuel fragments to the coolant, then a mixture is formed which when triggered, gives rise to violent steam explosion. This possibility is excluded in the model by assuming complete heat transfer during time of flight. Also the use of Rayleigh's solution for bubble expansion excludes shock wave effects.

For Stage 2, $t_0 < t < t_1$, $t = L/2C_{TP}$ is the time for the rarefaction to reach the ends of the pipe. Actually, Stage 2 should be bounded by $t_0 < t < 2t_1$ since flow at the break is influenced only when reflected wave returns to the break. For Stage 2 the fuel discharge velocity is given again by

$$U_{FO} = \frac{P_0 - P_b}{\rho_F C_F}$$

$$U_{FS} = \sqrt{\frac{2(P_0 - P_b)}{\rho_F}}$$

and the lower value (i.e., U_{FI}) is chosen to calculate the fuel and hydrogen discharge rate,

$$\dot{W}_F = 2 \rho_F U_{FI} A_F$$

$$\dot{W}_H = 2 \rho_H U_H A_0.$$

where $$U_H = \frac{P_0 - P_b}{\rho_H C_H}.$$

Once the discharge rates are known, the same procedure is used for the stream

generation rate and pressure transients.

For Stage 3, $t > t_1$, the flow from both sides of the break is treated separately. For the unblocked end, flashing of the liquid is assumed behind the reflected wave and the velocity is given by the expression

$$U_{H,INC} = \frac{P_f - P_b}{\rho_H C_H} [1 + INT(t/ta)]$$

$$t_a = \frac{2L_H}{C_H}$$

To calculate the strength of the reflected acoustic wave, we have to know the flashing pressure P_f. The report[10] says, given flashing volume V_f, and P_f, the quality X_F can be calculated. However it is not stated how V_f and P_f are obtained. It is not clear how the fuel discharge rate is computed for this stage. Presumably use is made of the same discharge velocity formula

$$U_{FO} = \frac{P_f - P_b}{\rho_F C_H} [1 + 2INT(t/t_a)]$$

$$U_{FS} = \sqrt{\frac{2(P_f - P_b)}{\rho_F}}$$

However, what speed of sound is to be used behind the flashing wave is not clear. If the fuel and hydrogen discharge rates are computed than the steam generation rate and pressure transients are computed as before. Note that the model erroneously assumes no flow during Stage 3 from the blocked end. Actually, as stated before, the flow discharge through the break is unaffected by the reflection until the reflected wave returns to the break itself.

For the dispersed flow model, Stage 1, $0 < t < t_0$ is identical to the stratified model. For Stage 2, $t_0 < t < t_1$ where $t_1 = L/2C_{TP}$ and

$$C_{TP} = C_g[\alpha^2 + \alpha(1 - \alpha) \frac{\rho_\ell}{\rho_g}]^{-1/2}$$

the flow velocity behind the pulse is taken to be

$$U_{MIX} = \frac{P_0 - P_b}{\rho_M C_{TP}}$$

where $\rho_M = \alpha\rho_H + (1 - \alpha)\rho_F$ and $\alpha = \frac{A_0}{A_0 + A_F}$

It is clear that the discharge flow from the break is normal to the flow accelerated by the acoustic wave, which is along the axis of the pipe.

However, to account for the directional change a pressure $P_1 = 1/2P_0$ was arbitrarily assumed. This arbitrary assumption cannot be justified! Assuming U_{MIX} is found, the fuel and hydrogen discharge rates are given by the expressions

$$\dot{W}_F = 2\ \rho_F\ U_{MIX}\ A_F$$
$$\dot{W}_H = 2\ \rho_H\ U_{MIX}\ A_0$$

The steam generation rate and pressure transients can be evaluated as before.

For Stage 3, no flow is assumed from the blocked end for $t>t_1$. One then waits until the unblocked end has completely discharged all its molten debris. Then it is assumed that the blocked end begins to empty its contents at the fully developed rate of the unblocked end. This assumption appears to be completely unrealistic.

For the unblocked end, the computation of the flow velocity employs the formula

$$U_{MIX} = \frac{P_0 - P_b}{\rho_M\ C_{TP}} \left[1 + 2\text{INT}\ (\frac{t}{t_a})\right]$$

The strength of the acoustic wave is still based on P_0 and P_b which is difficult to justify for Stage 3. However, assuming U_{MIX} is found, fuel and hydrogen discharge rates are computed from

$$\dot{W}_F = \rho_F\ A_F\ U_{MIX}$$
$$\dot{W}_H = \rho_H\ A_0\ U_{MIX}$$

and again steam generation and pressure transient are computed from previously given formulas.

In summary, it may be said that Henry's model[10] represents one of many possible models which could be used to describe vapor explosion. It completely excludes the condition under which violent steam explosion would occur. Even based on the heat transfer model assumed, shock wave effects are again excluded by taking only the steady state solution of Rayleigh for an expanding bubble in an incompressible liquid. There are no valid justifications for the model. It neither represents upper nor lower bound estimates of the consequences of a pressure/calandria tube rupture. It does not represent a most probable model either. Thus, whatever is predicted by the model, it cannot be used to represent what would happen following a postulated channel blockage accident in a CANDU reactor with pressure tube rupture and ejection of molten fuel into the

moderator because it does not address the FCI phenomenon. The model contains contradictions, inconsistencies and physically unrealistic assumptions. It is not clear how the computations are carried out and what additional input parameters that must be assumed to obtain the results for the nine cases computed. The validation of the model using only one set of data from Isles experiment is inadequate. Vapor explosion experiments are themselves notoriously irreproducible. One needs an ensemble of similar experiments for statistical results. Even with all the freedom to adjust the various parameters in the theory, the agreement between Henry's prediction and Isles experimental results is still out by a factor of two. It is difficult to justify the authors' claims that good agreement with experiments gives them confidence to the theory to other accident conditions in the CANDU reactor system.

III. CONCLUSIONS

Energetic high yield steam explosions capable of inflicting severe structural damage cannot be regarded as an improbable event when large quantities of molten fuel and coolant are mixed together. There are sufficient actual industrial accidents and large scale experiments to substantiate the above statement. In a hypothetical flow blockage event followed by core melt and subsequent discharge of molten fuel into the moderator, coarse fragmentation and dispersion to form a mixture followed by triggering and propagation of a vaporization wave is a possible scenario. Assessment of the current state-of-the-art indicates that the precise initial conditions necessary for coherent propagation of a vaporization wave (hence an energetic steam explosion) through the coarse fragment-coolant mixture has not yet been established. This is considered as the most important piece of information to be determined by further research. The Henry model at its best describes only one possible sequence of events following flow blockage. No convincing arguments have been found to conclude that Henry's model represents the most probable or realistic situation. Henry's model negates a-priori, the possibility of an energetic high yield steam explosion by assuming fine fragmentation and rapid heat transfer of the discharge fragments to form steam. Detailed analysis of Henry's model (as presented in Ref. 10) reveals that there exist a large number of inconsistencies and errors that need to be

clarified prior to any meaningful discussions of the results generated from the model itself.

IV. RECOMMENDATIONS AND PROPOSAL OF A MODEL

From the study carried under the terms of this contract, the following recommendations are proposed.

1. Recommendations:

(1) Leaving aside all comments regarding the validity of the model[10] it is important to clarify the analysis and the computational procedures so that the results for the nine test cases obtained can be assessed properly within the context of the model used. This is not possible at present since there is too much information missing to permit someone else to reproduce the results. I recommend, therefore, that authors of the report[10] provide a detailed step-by-step procedure, listing all the equations used, explaining what and how the various input parameters are used in the computation, and showing a detailed sample calculation of two of the nine cases. It is suggested that Case 1, using the stratified model, and one other case, using the dispersed model, be selected.

(2) It is recommended that an independent organization be asked to review carefully the procedures provided and write an independent computer program to duplicate the results of those nine test cases. It is judged that the computer program will not be unnecessarily complicated in view of the rather simple algebraic equations involved. Thus it is not recommended to ask the Authors to provide their program for this purpose. It is more reliable if results are obtained using an independent program.

(3) Having acquired the ability to duplicate the results of the model, and having precise understanding of the sensitivity of the various input parameters used on the final results, a more in-depth assessment of the results of the nine test cases can be made with confidence. The validity of the conclusions within the context of the model assumed can then be determined.

(4) Pending the results obtained after the computational procedures, and after determining the sensitivity of the results on arbitrary input parameters, the model itself should then be subjected to detailed scrutiny as to the assumptions made. If the model is found to be adequate then, perhaps, efforts devoted to replacing some of the assumptions by more realistic ones, and eliminating some assumptions altogether by modifying the model itself, can be made. All this is dependent on whether the model is found to be worth saving.

(5) Based on the information surveyed in this review it is doubtful that one can completely exclude the possibility of a violent vapor explosion in a situation where hot molten fuel gets in contact with cold liquid. Steady state models that ignore compressibility effects and shock waves are not realistic for predicting violent vapor explosion. Hicks and Menzies type of thermodynamic models fail to provide the proper peak pressure transients and impulse applied to adjacent structures. Thus the development of a hydrodynamic shock wave model, capable of describing the peak pressure transients associated with a violent explosion event, is highly recommended. This model should provide an upper bound for the peak over-pressures and impulse generated.

(6) In view of the current state-of-the-art in the understanding of the mechanisms of vapor explosion, it is highly recommended that a three-year experimental program be initiated to clarify the more important processes so that necessary and sufficient conditions for violent vapor explosion to occur can be formulated. In particular, it is necessary to establish the required initial conditions, i.e., the triggering mechanism and the propagation mechanisms. It is recommended that flash X-ray photography be used to observe the structure of self-sustained propagating explosion fronts to determine the mechanisms. Detailed measurements of the dynamic pressure field associated with steam explosions should also be studied in this program.

2. Proposed Model:

A brief description of the philosophy and requirements for a realistic model to estimate the peak over-pressures and impulse from a vapor explosion is given below.

The objective of the model is to give an upper bound estimate for the peak pressure transients. Thus it is not necessary to be concerned with the details of the actual discharge process of the molten fuel through the break itself. Since the blast pressure field is associated with the energy of the blast and the energy is derived from the internal energy of the molten fuel, it is necessary to specify the amount of fuel involved as well as the initial internal energy of the fuel. Next it is assumed that the fuel fragments are mixed with a certain quantity of moderator coolant. There is the stable vapor film boiling criterion to "lock in" the thermal energy during fragmentation and dispersion, so it is not necessary to be concerned with the details of the fragmentation and dispersion process. We must bear in mind that we seek the optimum initial conditions for a violent steam explosion. Thus we shall assume the formation of a certain volume of fragment-coolant mixture. The amount of coolant in the mixture is an important factor. As in the case of the Hicks and Menzies model, we see that the optimum work potential is a function of the fuel to coolant mass. Thus this shall be an important parameter in the model.

Next we shall assume that the volume of fragment-coolant mixture is triggered and the coolant in the volume is "instantaneously" converted to the vapor state. Thus we may calculate the initial pressure and temperature in this volume subsequent to the trigger. The events that follow will be the expansion of this volume driving ahead a shock (blast) wave. There will be various cases to be investigated in terms of the assumption made regarding the conditions inside the expanding bubble. For example, we may asume an adiabatic expansion, and equilibrium expansion with condensation corresponding to the local thermal dynamic states. Both such "thermodynamic paths" for the expanding vapor have been considered in the Hick and Menzies equilibrium model.

The expansion of the shock wave and the associated pressure field will have to be determined by a solution of the non-linear hydrodynamic equations. Thus a complex numerical code will have to be written for this purpose.

With the objective of obtaining the upper bound pressure field, it is proposed to investigate several energy release rates (i.e., bubble expansion

rates). These can be arbitrarily assumed to seek the optimum condition. If the time duration of interest is long, the expanding bubble would undergo oscillatory motions in the moderator, and a separate phase of the work must be initiated to investigate the pressure field associated with this bubble oscillation.

Finally, to account for geometric effects both spherical as well as cylindrical explosions should be investigated to bracket the possible shape of the volume of the fuel-coolant mixture which depends on the shape of the rupture opening of the pressure/calandria tube.

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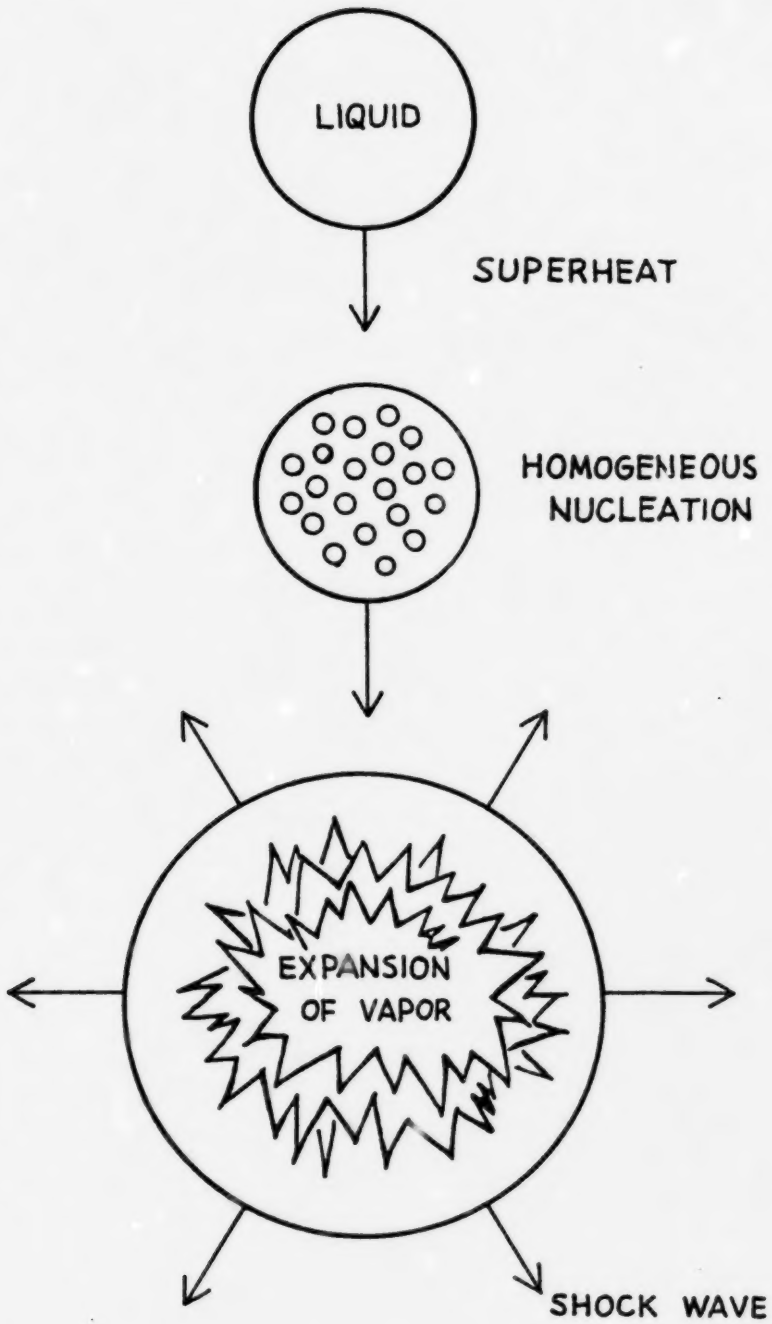


FIG. 1

IDEALIZED VAPOR EXPLOSION

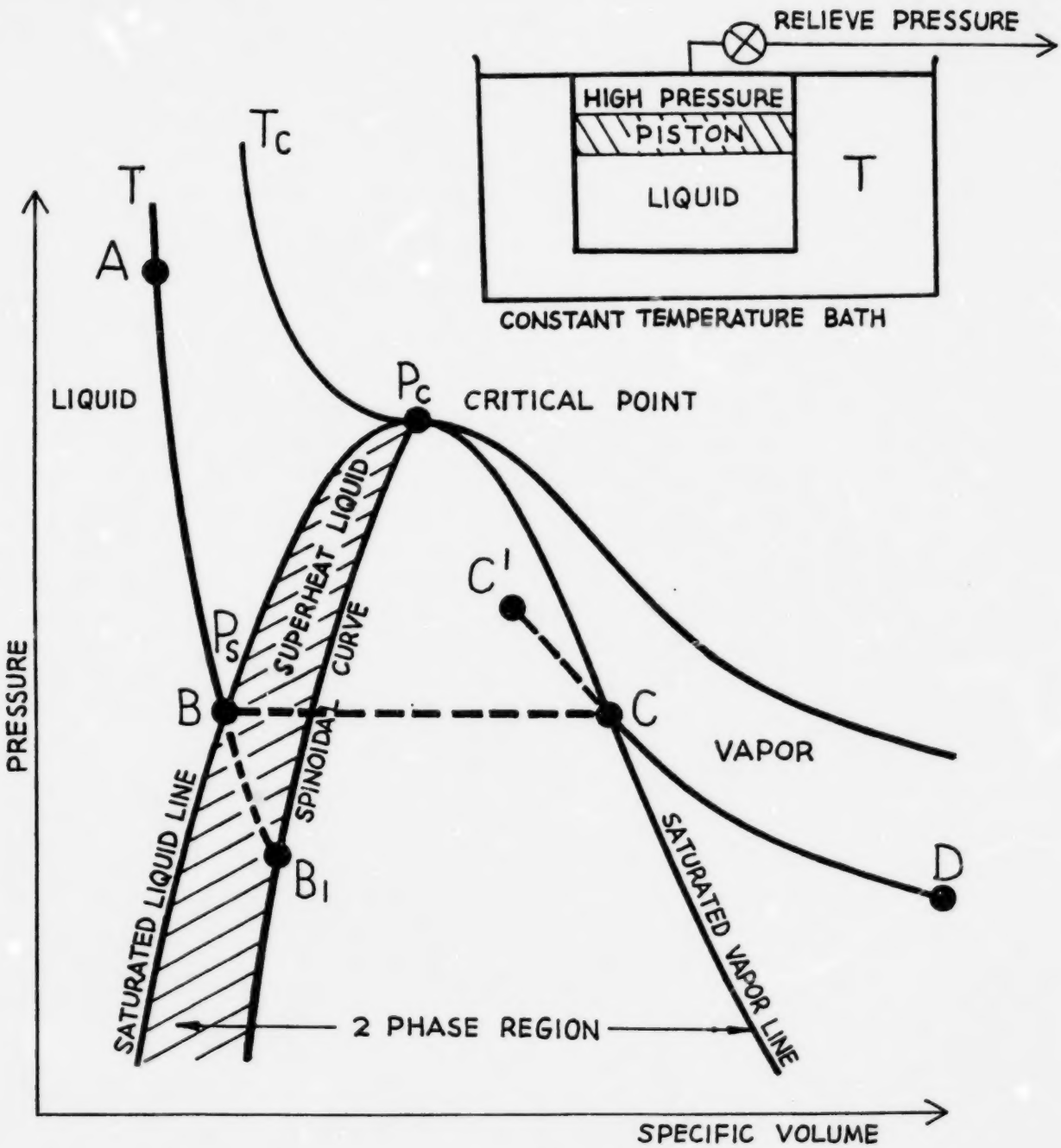


FIG. 2

ISOTHERMAL DEPRESSURIZATION

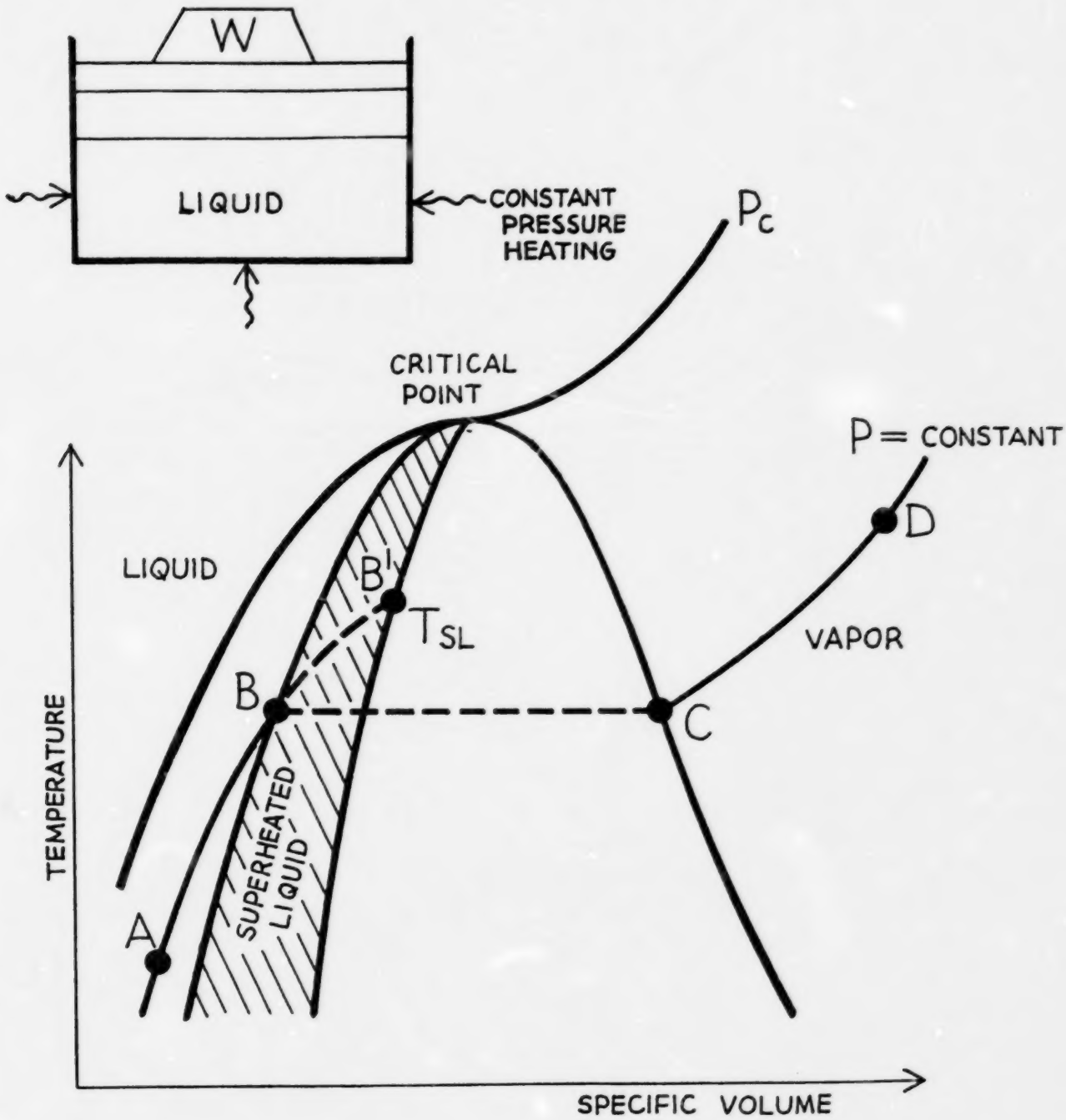


FIG. 3

CONSTANT PRESSURE HEATING

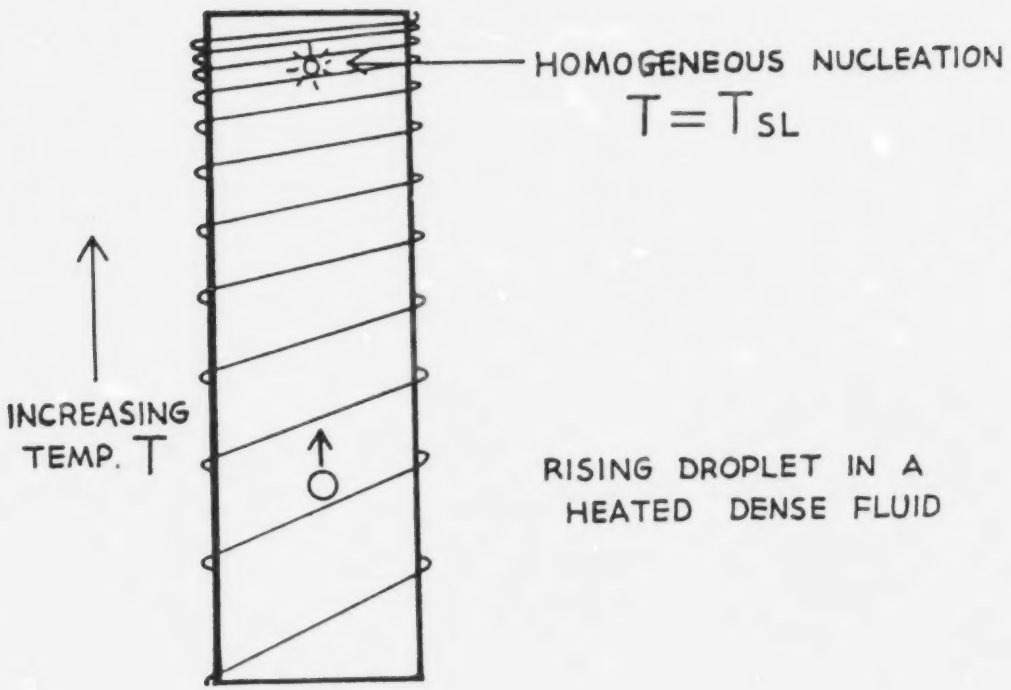
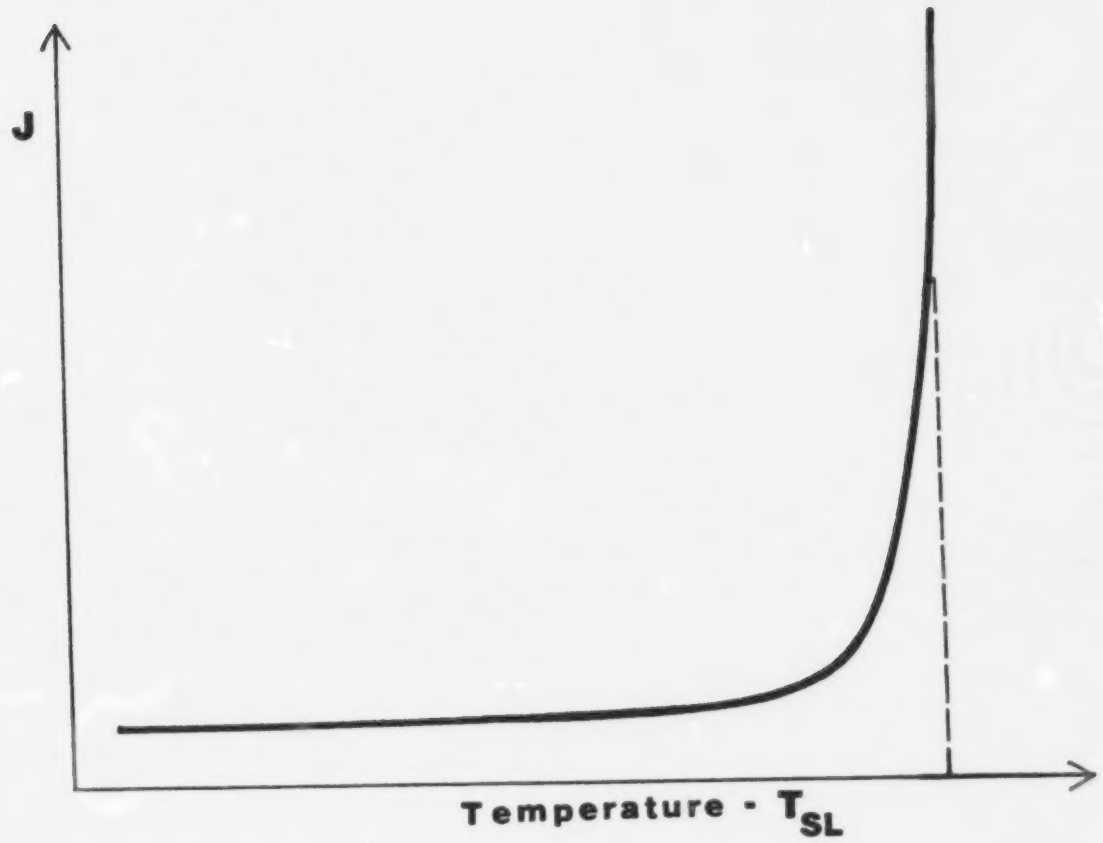


FIG. 4

EXPERIMENTAL DETERMINATION OF T_{SL}.



$$T_{SL} = \frac{16\,\pi\,\sigma^3}{3(P_v - P_L)^2\,K}$$

FIG. 5

VARIATION OF NUCLEATION RATE J AS A FUNCTION OF TEMPERATURE.

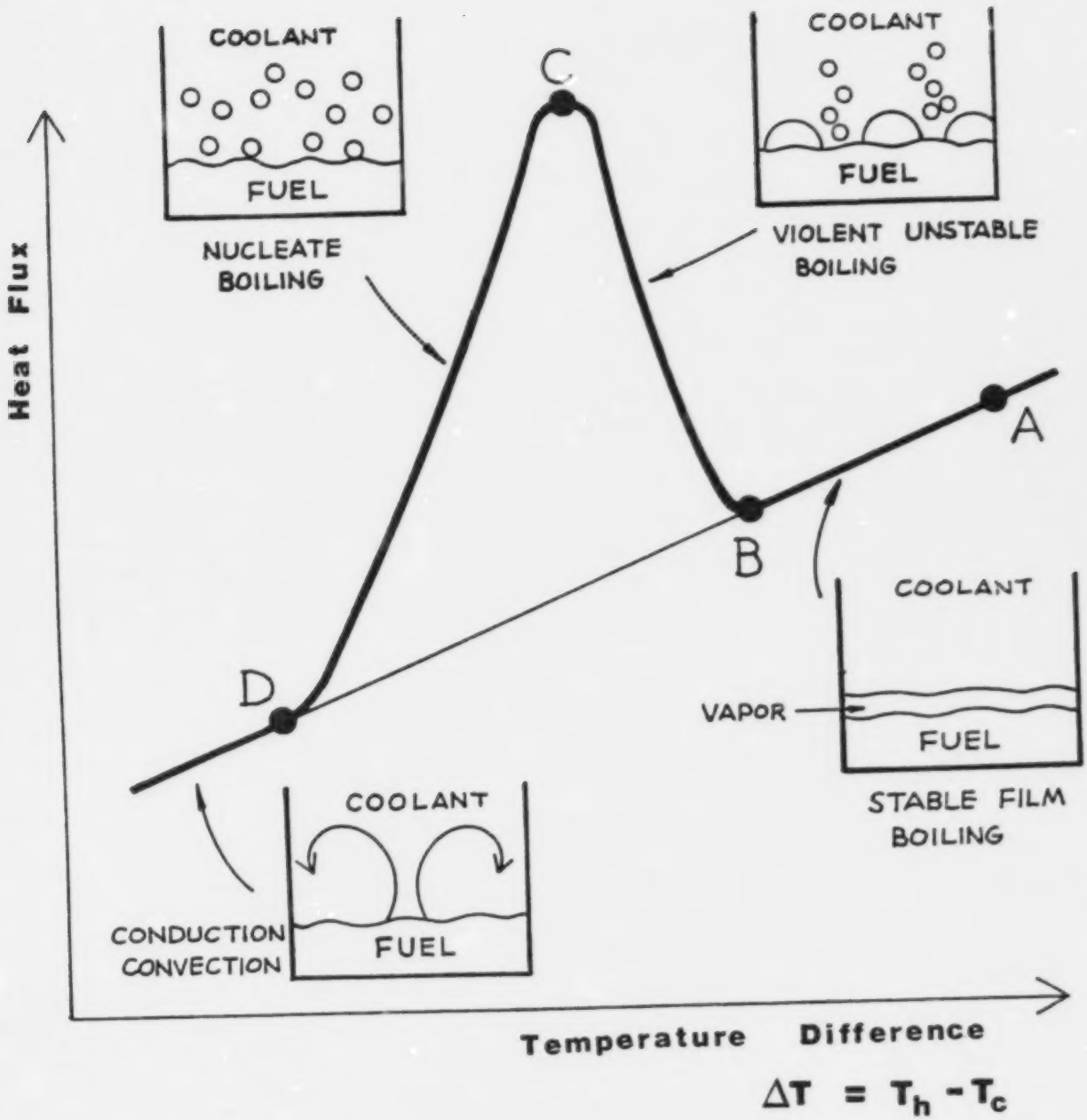


FIG. 6

VARIATION OF HEAT FLUX WITH TEMPERATURE
UNDER CONDITIONS OF BOILING.

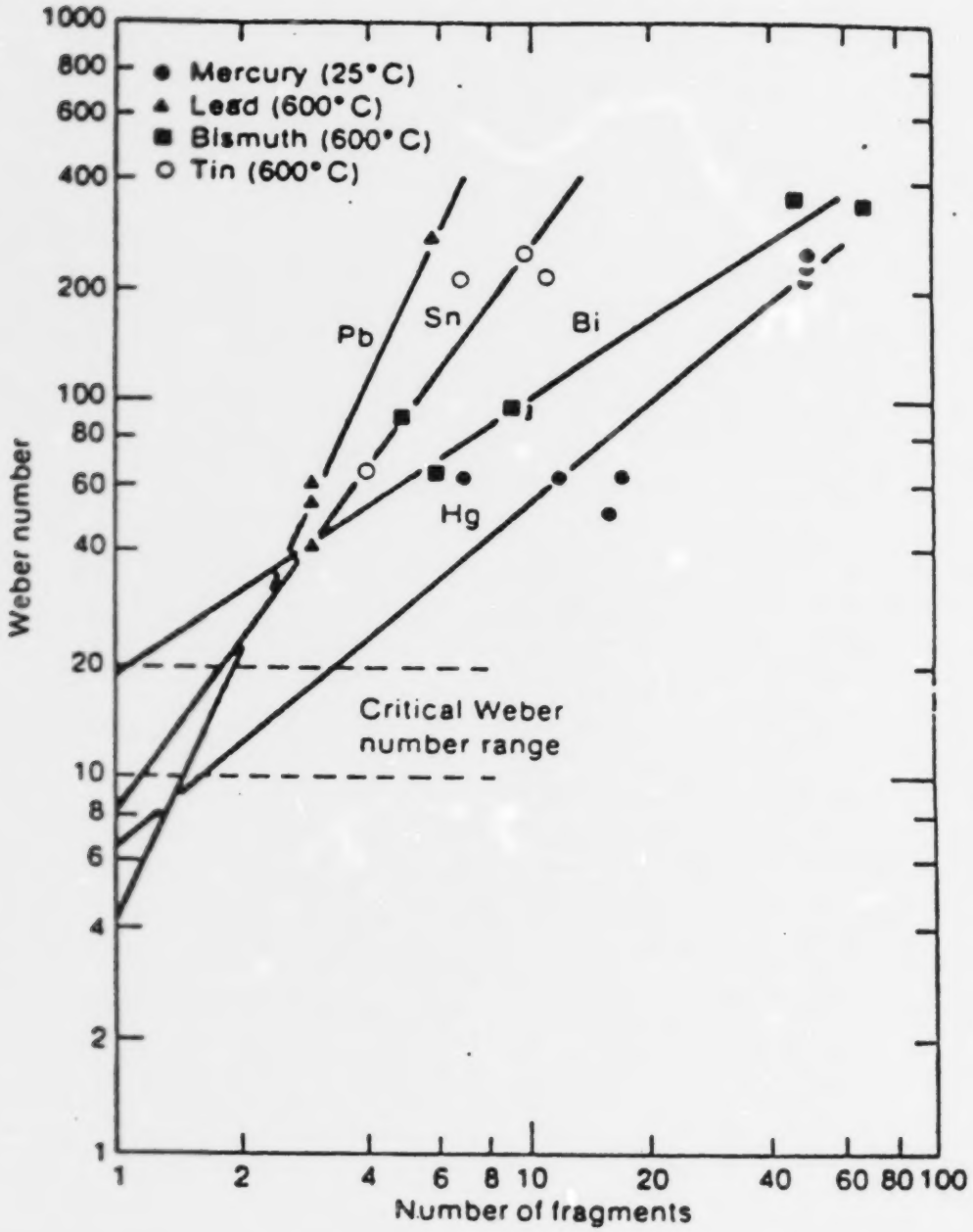


FIG. 7

PLOT OF WEBER NUMBER VERSUS NUMBER OF FRAGMENTS FOR MOLTEN METALS DROPPED INTO ROOM-TEMPERATURE WATER (REF. 1).

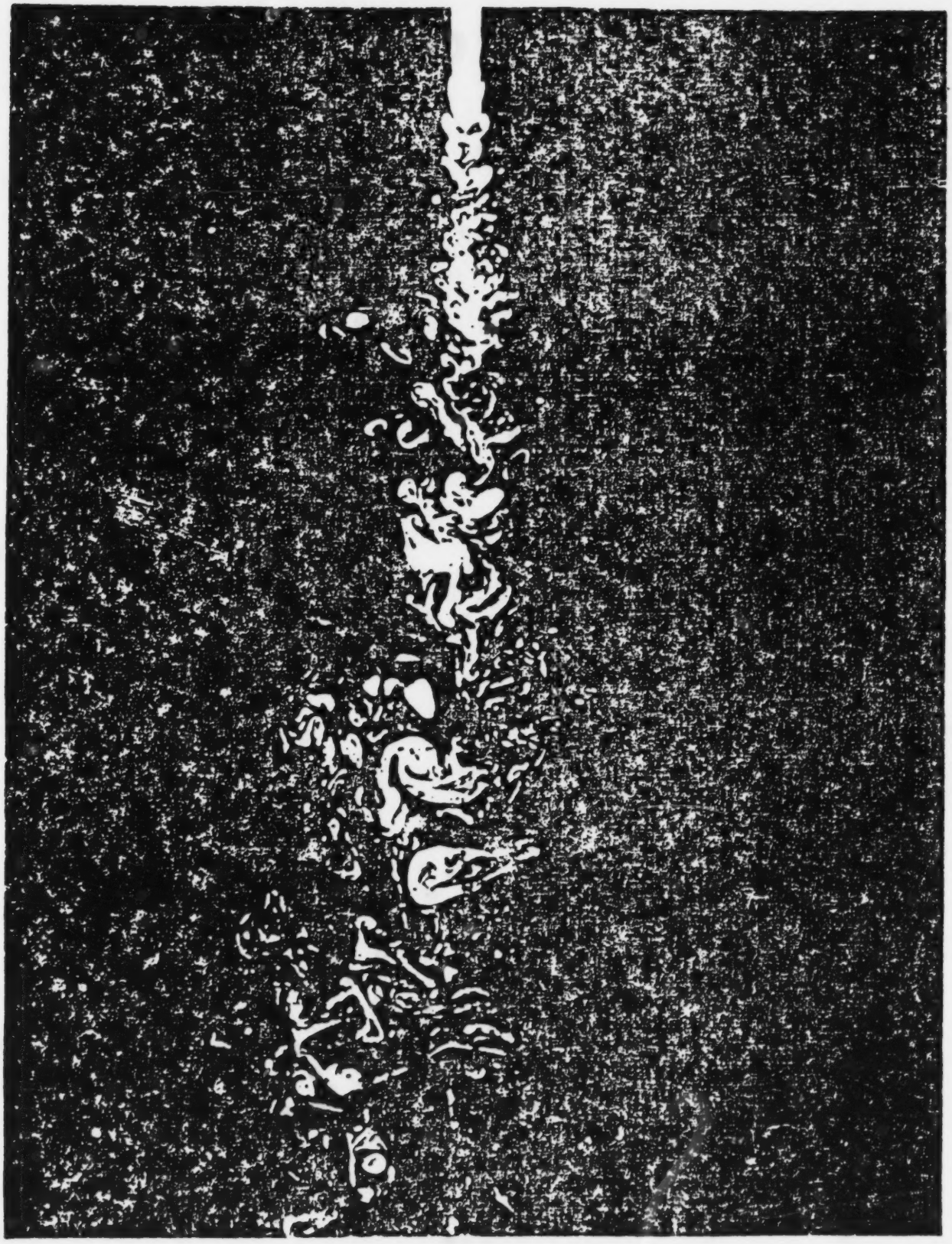


FIG. 8. PLOT OF WEBER NUMBER VERSUS NUMBER OF FRAGMENTS FOR MOLTEN METALS DROPPED INTO ROOM-TEMPRATURE WATER (REF.1).

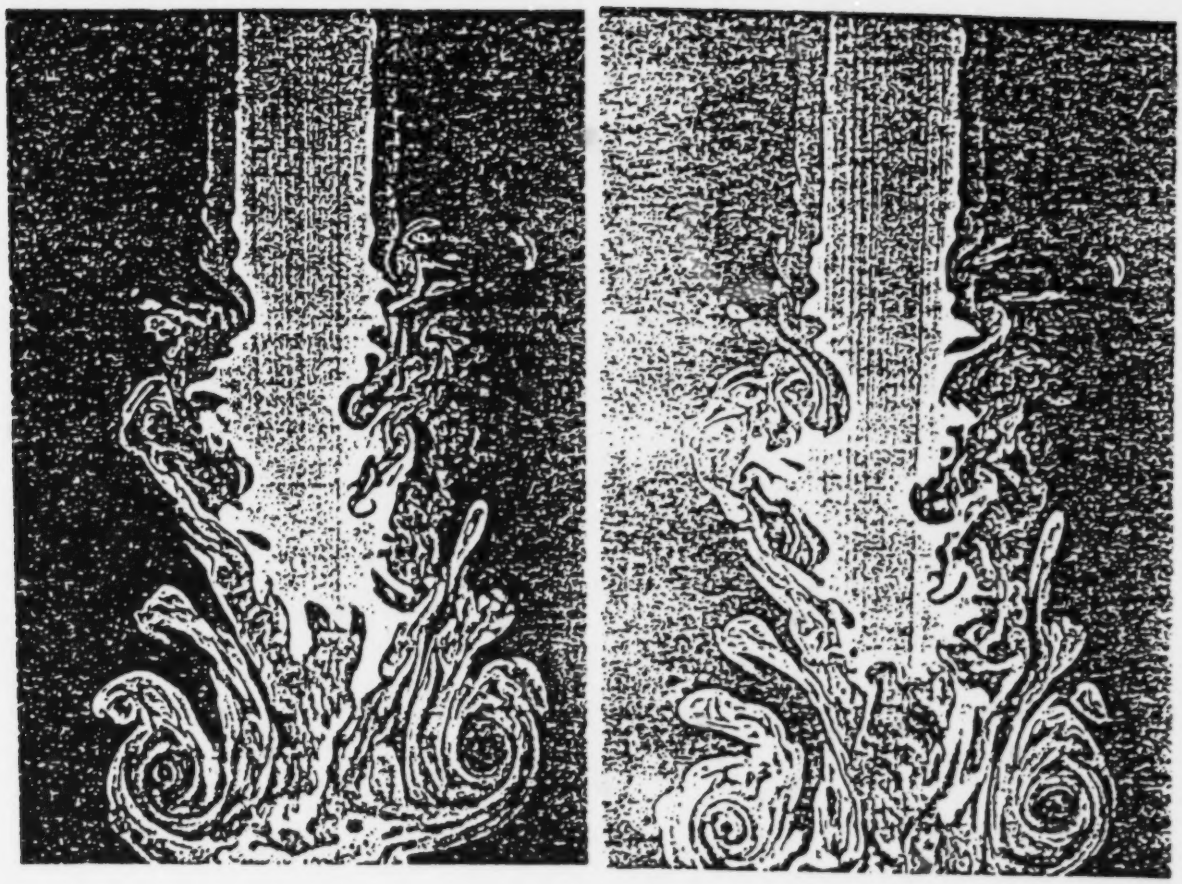


FIG · 9

PENETRATION OF A LIQUID JET INTO STATIONARY LIQUID. (COURTESY: P. DIMOTAKIS, CALTECH).

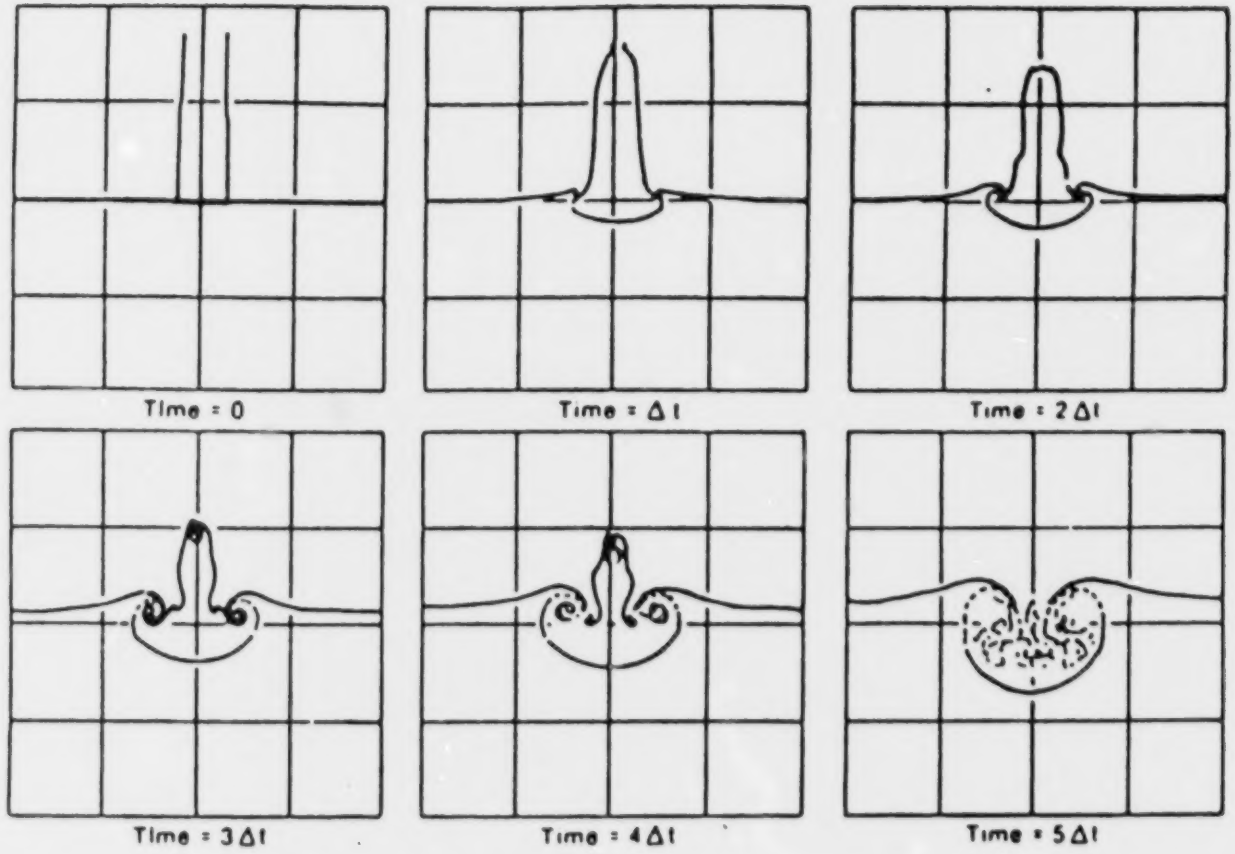


FIG. 10

COMPUTER SIMULATION OF LIQUID JET PENETRATION INTO ANOTHER FLUID

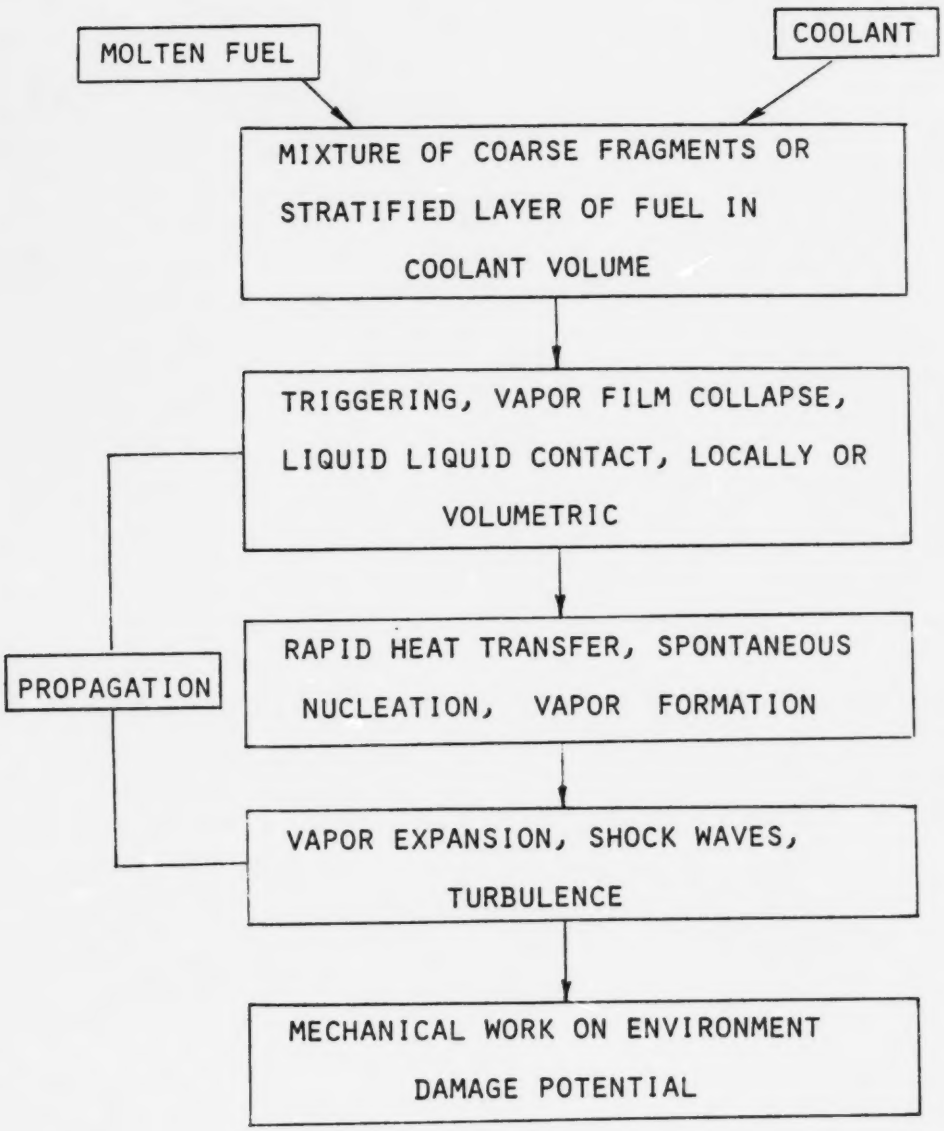

MOLTEN FUEL
COOLANT
MIXTURE OF COARSE FRAGMENTS OR STRATIFIED LAYER OF FUEL IN COOLANT VOLUME
TRIGGERING, VAPOR FILM COLLAPSE, LIQUID LIQUID CONTACT, LOCALLY OR VOLUMETRIC
PROPAGATION
RAPID HEAT TRANSFER, SPONTANEOUS NUCLEATION, VAPOR FORMATION
VAPOR EXPANSION, SHOCK WAVES, TURBULENCE
MECHANICAL WORK ON ENVIRONMENT
DAMAGE POTENTIAL

FIG. 11

SEQUENCE OF EVENTS IN VAPOR EXPLOSION

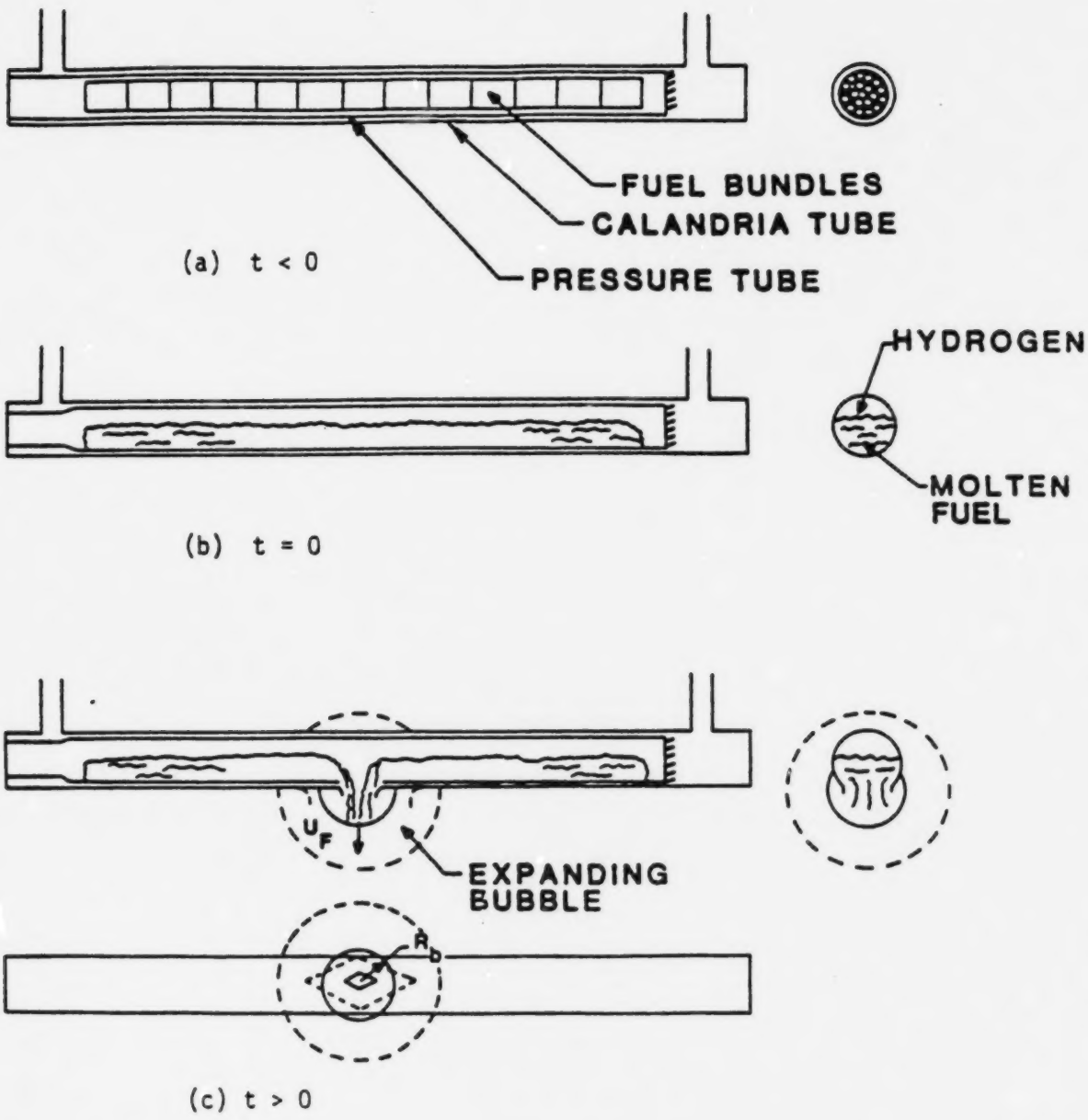


FIG. 12

EVENT SEQUENCE FOR STRATIFIED FLOW MODEL.

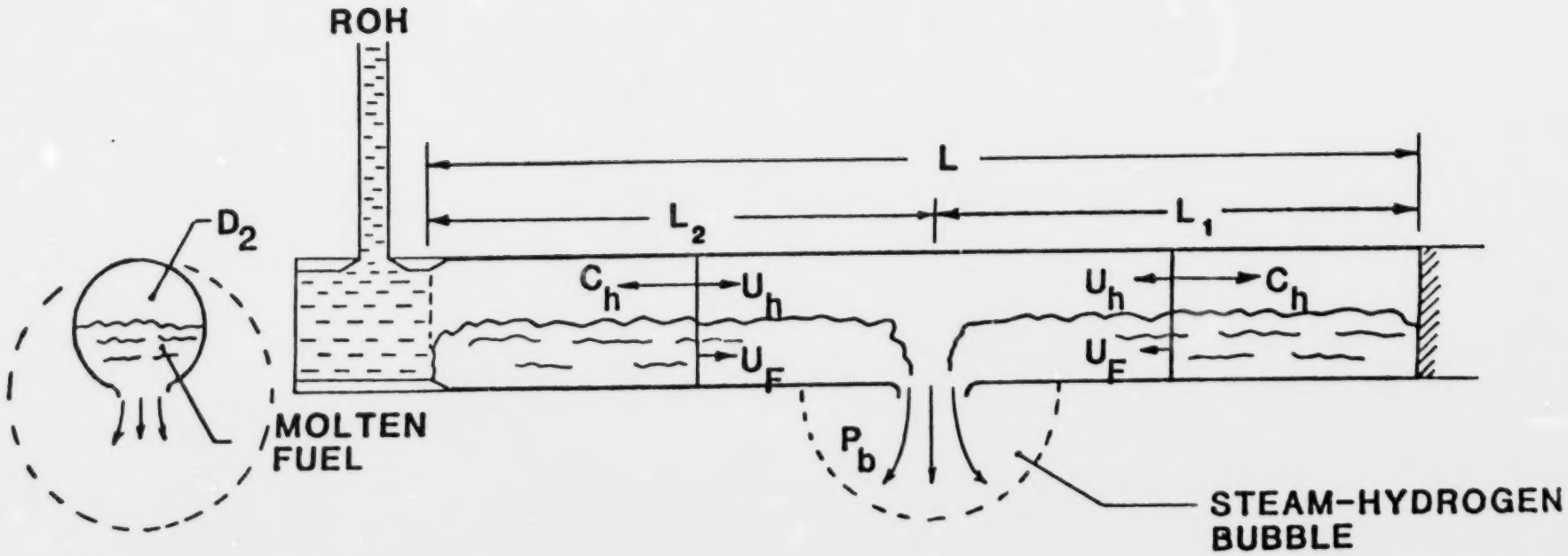


FIG. 13

STRATIFIED MODEL STAGE 2.

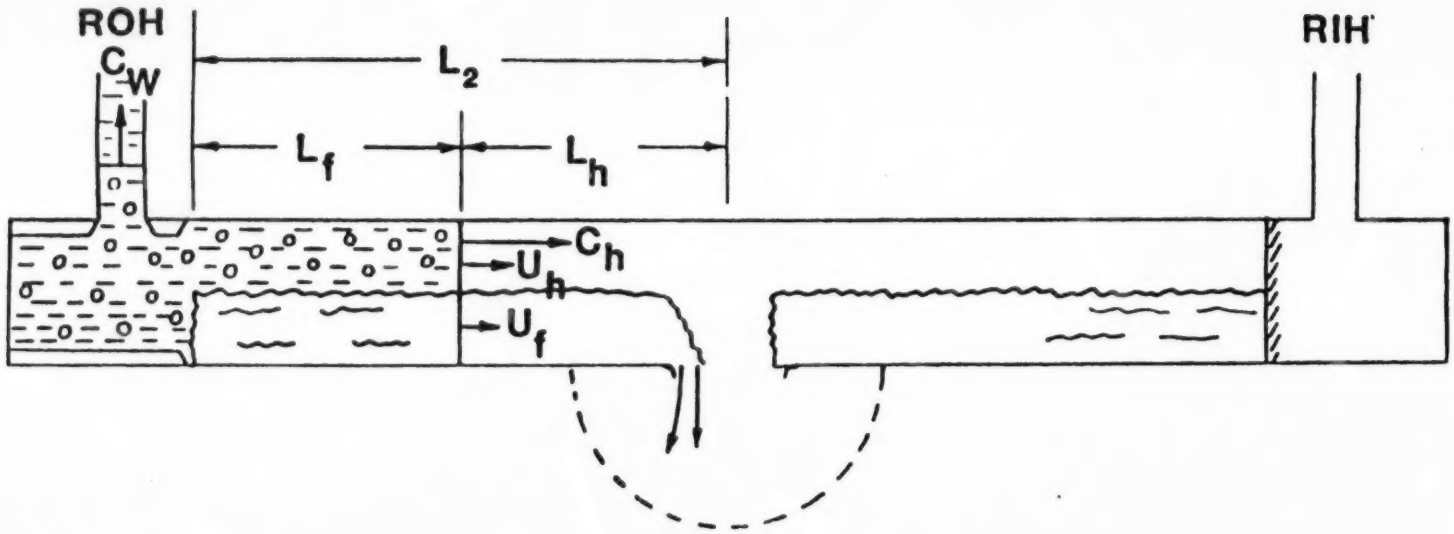


FIG. 14

STRATIFIED MODEL STAGE 3.

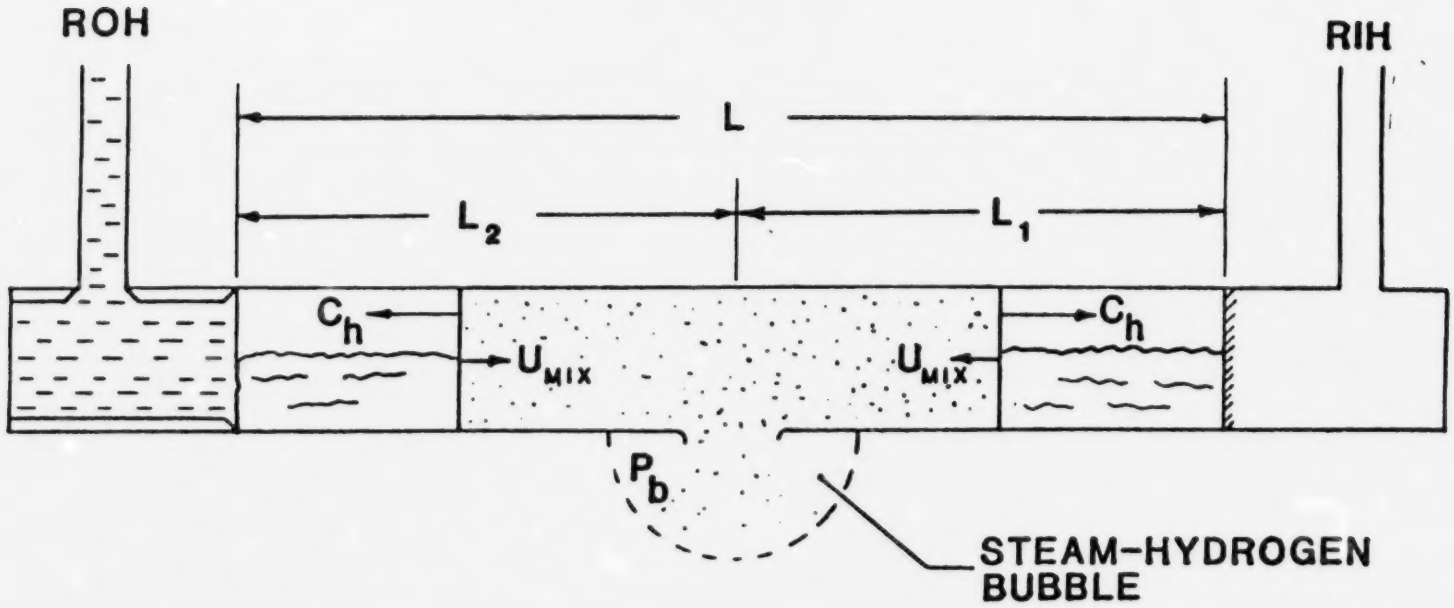


FIG. 15

DISPERSED MODEL STAGE 2.

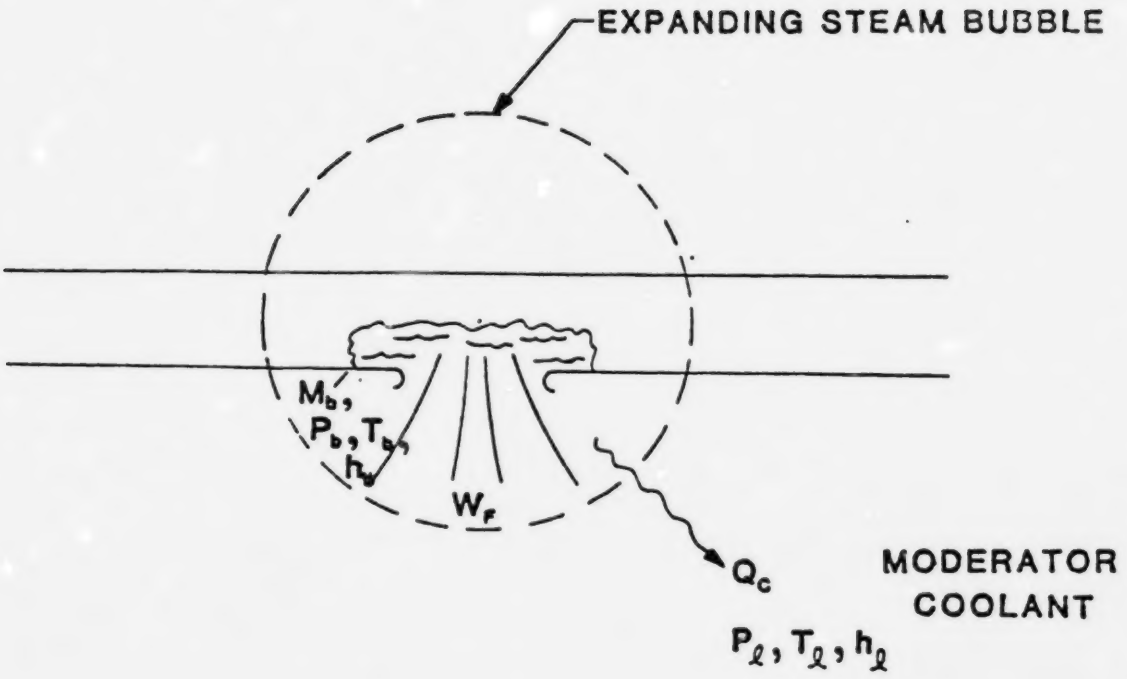


FIG. 16

ENERGY BALANCE.

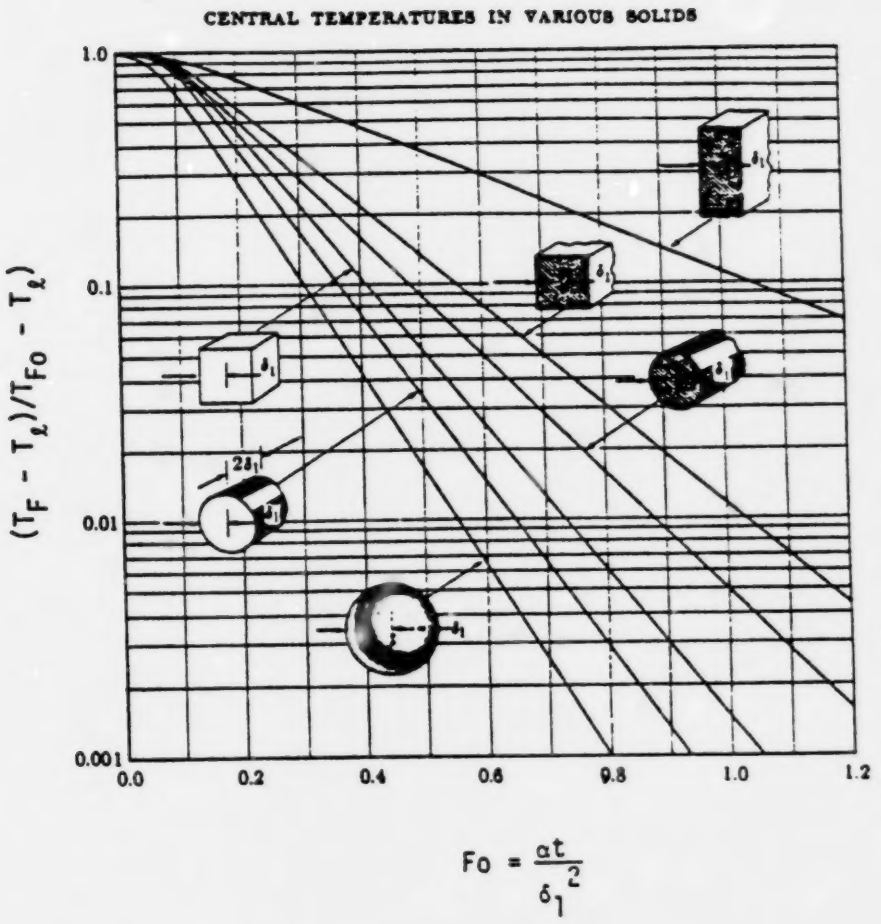


FIG. 17

CENTRAL TEMPERATURE HISTORY IN A SPHERE

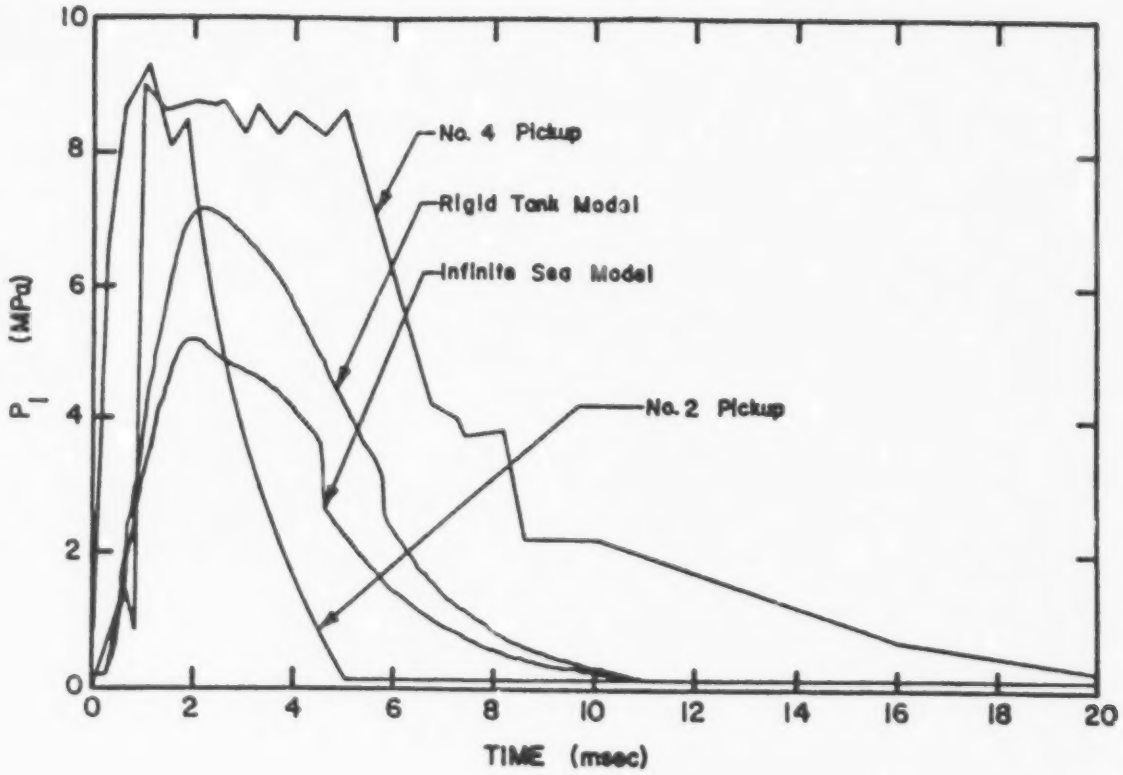


FIG. 18

COMPARISON OF MODEL PREDICTIONS WITH EXPERIMENTAL MEASUREMENTS FOR NO. 2 AND NO. 4 PICKUPS AT 0.292 M RADIAL POSITION.

Case No.	Material Temperature (K)	Material Available for Ejection (% channel inventory)	Rupture Length (m)	Dimensionless Quenching Time	Material Discharge Model	Available Collapse Volume (m^3)	Moderator Volume (m^3)	Duration Stage 1 (ms)
1	2473	100	.42	1	Stratified	3.5	280	1.0
2	2473	100	.42	1	Dispersed	3.5	280	1.0
3	2473	50	.42	1	Dispersed	3.5	280	1.0
4	2473	100	.42	1	Dispersed	5	280	1.0
5	2473	100	.42	1	Dispersed	3.5	240	3.3
6	2473	100	2.0	1	Dispersed	3.5	280	1.0
7	3273	100	.42	1	Dispersed	3.5	280	1.0
8	3273	100	.42	1	Dispersed	3.5	240	1.0
9	3273	100	.42	1.2	Dispersed	3.5	240	1.0

TABLE I

SUMMARY DESCRIPTION OF HYDRODYNAMIC TRANSIENT CASES.

APPENDIX

ESTIMATE OF POTENTIAL DAMAGE BY STEAM EXPLOSION

APPENDIX: ESTIMATE OF POTENTIAL DAMAGE BY STEAM EXPLOSION

1. INTRODUCTION

This section of the report is in response to a request by AECB staff to carry out a number of calculations of steam explosion using the Hicks-Menzies model as applied to a typical CANDU reactor calandria vessel. The calculations are intended to simulate the effects of several hypothetical core melt situations. The envisioned scenario is the partial or total blockage of coolant to one fuel channel resulting in melting anywhere from 25 kg to a maximum of 300 kg of fuel material. The thermal interaction of the molten material with the moderator coolant can potentially lead to steam explosion. If the calandria vessel is breached, then the moderator coolant will vaporize explosively to a background vented pressure of one atmosphere (ambient). In this scenario, one is interested in the maximum work potential ΔW of the steam explosion. If the calandria vessel remains intact, then the steam explosion will cause an increase in the pressure by compressing the remaining non-vaporized moderator coolant within the fixed volume of the calandria vessel. In this scenario, the moderator coolant on interaction with the molten material will vaporize explosively to a background pressure, depending on the compressibility of the bulk of the moderator coolant in the intact calandria vessel. One is interested in this case in the maximum final steam explosion pressure p_f inside the calandria vessel. Schematically, the two situations are illustrated in Fig. A.1.

2. HICKS-MENZIES MODEL

The Hicks-Menzies model is purely an equilibrium thermodynamic (static) accounting of energy changes in the vapor-liquid phase region of the Mollier phase chart for the coolant fluid. The model involves no dynamic effects such as instability, turbulence, fragmentation, rates of energy flux, etc. It assumes that the molten core transfers its heat to the surrounding moderator coolant without doing any work. The affected region of moderator coolant, as a consequence, finds itself in the supercritical state. It then boils explosively and drops into its final equilibrium state in the vapor-liquid region of the phase diagram. Schematically, one can illustrate the

thermodynamic path of the moderator coolant on the Mollier phase diagram from initial to final state for the two cases in Fig. A.2.

a) Initial conditions CANDU Reactor

i) Core melt: The core melt is composed of uranium dioxide (UO_2) fuel and Zircaloy cladding. The melting point for Zircaloy is 2123°K and the melting point for the UO_2 fuel is 3073°K. To be conservative, the temperature of the molten material is taken to be T_m = 3073°K. The maximum amount of molten material is taken to correspond to the total content of one fuel channel which corresponds to a mass m_m = 300 kg. The specific heat of the molten material depends on the relative fraction of Zircaloy and fuel in the melt. The specific heat for Zircaloy ~0.35 kJ/kg°K; the specific heat for uranium dioxide is C_v ~ 0.65 kJ/kg°K. Since the proportions of Zircaloy and fuel in the core melt are different for different reactors then a value of specific for the core is taken as C_{v_m} = 0.560 kJ/kg°K.(1)

ii) Moderator coolant: The moderator coolant is heavy water (D_2O). The total calandria contents depend on the reactor considered. The Pickering reactor has a calandria volume V_c = 242 m^3. This corresponds to a total moderator mass of M_c = 242 m^3 x 1090 kg/m^3 = 263,780 kg. For the Bruce reactor the volume V_c = 284 m^3 which corresponds to a mass M_c = 309,560 kg. For a conservative estimate in a steam explosion, the lower value is taken in the calculations.

$$M_c = 263{,}780 \text{ kg of } D_2O$$

The calandria coolant temperature is taken as the mean value between the entry and the exit.

$$T_c = \frac{43 + 71}{2} = 57°C = 330°K$$

The moderator coolant pressure is taken as the mean value between the entry and the exit.

$$P_c = \frac{34.5 + 107.6}{2} = 71.05 \text{ psig} = 172.4 \text{ kPa(abs)}$$

$$p_c = 0.1724 \text{MPa (absolute)}$$

b) Core Melt-Moderator Interaction-Equilibrium Temperature T_e

In the Hicks-Menzies model it is assumed that mass m_m of the core melt interacts thermally with mass m_c of the coolant. The mass m_m is usually well defined. For most calculations in this report m_m = 300 kg was used. The mass m_c, however, is arbitrary since we do not know how much of the moderator coolant is "effectively" affected by the molten core. In any case, the calculation can be performed parametrically in terms of m_c/m_m and then the appropriate choice for the relevant value of m_c/m_m can be assumed based on the particular circumstances.

The interaction of the molten core m_m with the affected moderator mass m_c is a constant volume heat exchange with no work done. Schematically, a quantity of heat $\Delta Q_m = \Delta Q_c$ is transferred at constant volume from the molten material to the moderator coolant (see Fig. A.3). In the heat transfer process, the molten core of mass m_m cools from T_m to the equilibrium temperature T_e. Simultaneously, the affected mass m_c of the moderator coolant heats up from T_c to T_e at constant volume. No work is involved, such that the first law of thermodynamics gives

$$\Delta U = \Delta Q - \Delta W \tag{A.1}$$

with $$\Delta W = 0$$

For the heat given up by the molten material

$$\Delta Q_m = -\Delta U_m = -m_m C_{V_m} [T_e - T_m] \tag{A.2}$$

For the heat received by the moderator coolant

$$\Delta Q_c = \Delta U_c = m_c C_{V_c} [T_e - T_c]$$
$$u_e - u_c = C_{V_c} [T_e - T_c] \tag{A.3}$$

Since $\Delta Q_m = \Delta Q_c$, this immediately yields that

$$T_e = \frac{\frac{m_m C_{V_m}}{m_c C_{V_c}} T_m + T_c}{1 + \frac{m_m C_{V_m}}{m_c C_{V_c}}} \tag{A.4}$$

The equilibrium temperature T_e is plotted parametrically in terms of $y = m_c/m_m$ in Figure A.4. To illustrate the effect of the assumed "affected" mass of the moderator coolant mass in the calandria (M_c) one can estimate some

limiting cases. Suppose that the mass of the core melt is taken as m_m = 25 kg to simulate partial melting of the fuel. If the "affected" moderator coolant mass is taken to be the entire contents of the calandria M_c = 263,780 kg, then the calculation would give T_e = 330.035° or a temperature increase of $\Delta T \sim .035$ °K above the initial temperature of the coolant. This is insignificant. Even if the entire fuel in one channel is assumed to melt yielding m_m = 300 kg, and if this is taken to interact with the entire coolant contents of the calandria the equilibrium temperature is only 330.4 °K or a ΔT of 0.4 °K. This is still insignificant. In fact, Figure A.4 indicates that until one reaches $m_c/m_m < 10$, there is an insignificant increase in the temperature. Thus, it is unrealistic to consider the possibility of steam exlosion for $m_c/m_m > 10$.

c) Steam Explosion Work Potential

The calculation in this section is for the steam explosion work potential ΔW which corresponds to Case 1 of Fig. A.1. The heated moderator coolant at T_e now expands into the vapor-liquid region to atmospheric pressure and a final state (2). The expansion of the moderator coolant from T_e to T_2 is assumed isentropic, hence

$$S_2 = S_e = S_1 + C_V \ell n\, T_e/T_1 \tag{A.5}$$

S_2 can also be written in terms of the saturated liquid-vapor properties and the dryness fraction x as follows

$$S_2 = S_f + x_2\, S_{fg} = S_1 + C_V \ell n\, T_e/T_1 \tag{A.7}$$

Since S_f and S_{fg} are given in the heavy water steam tables, x_2 can then be calculated.

One can then calculate the specific internal energy u_2 using the D_2O steam tables and the following relationship

$$u_2 = u_f + x_2\, u_{fg} \tag{A.8}$$

The work potential is the energy change in the vaporization process from state (e) to state (2) or

$$\Delta W = m_c\, [u_e - u_2] \tag{A.9}$$

which can now be calculated since u_e and u_2 are known. The work potential ΔW for steam explosion has been calculated for the CANDU reactor for m_m = 300 kg as a function of $y_c = m_c/m_m$ and is displayed in Fig. A.5. The results indicate that ΔW_{max} = 180 megajoules or $\Delta W/m_m$ = 0.600 megajoules per kilogram of molten

material. The maximum work potential occurs at about a value of $y = m_c/m_m \sim 0.4$. The results are in agreement with the calculations of Corradini[2] for comparable scale of Light Water Reactor (LWR) conditions. Corradini's work shows that if isentropic expansion of the mixture is assumed (i.e., the coolant expands and the molten fuel transfers heat to the coolant during expansion process), the ΔW_{max} is about 35% higher. The mixture expansion case was not done in the present study, only the coolant expansion was considered. However, Corradini's calculations can be used to scale the effect (i.e., a factor of 1.35). The work potential can be written as

$$\Delta W = \Delta KE + \Delta PE + p_0 V_S \qquad (A.10)$$

The ΔKE is the kinetic energy available to launch projectiles. The change in potential energy ΔPE is usually negligible. The volume of the spilled coolant is V_S and $p_0 V_S$ represents the work done against the atmosphere by the ejection of coolant from the breached calandria vessel due to the steam explosion.

d) <u>Steam Explosion Pressure Rise-Intact Calandria</u>

This section deals with the maximum pressure rise due to steam explosion for an intact calandria and corresponds to Case 2 of Fig. A.1. In this case, the moderator coolant at T_e expands into the vapor-liquid region to some pressure p_f which depends on the compressive deformation yield of the unaffected moderator coolant in the constant volume of the calandria. To determine this pressure, one uses the expression for the calandria volume V_C in terms of the properties of the contents. Thus, initially the state is (1)

$$V_C = M_c [v_{cl}]_{p=p_1} \qquad (A.11)$$

where M_c is the total mass of coolant in the calandria vessel. Also for the final state

$$V_C = [M_C - m_c][v_{cl}]_{p=p_f} + m_c [v_f + x_2 v_{fg}]_{p=p_f} \qquad (A.12)$$

In equation (A.12), the first term corresponds to the volume of the compressed "unaffected" coolant $[M_C - m_c]$ while the second term represents the volume of the vapor-liquid of the affected mass of coolant m_c which has undergone a steam explosion to the final pressure p_f in the confined calandria. Thus, the final pressure p_f can be determined by trial and error. A value of p_f is guessed, then $[v_{cl}]_{p=p_f}$, $[V_f]_{p=p_f}$, $[v_{fg}]_{p=p_f}$ are calculated or read off

from heavy water steam tables. Iteration proceeds until equation (A.12) is satisfied. At the request of AECB staff, p_f was determined as a function of m_c/m_m for m_m = 300 kg. Three cases were considered: calandria volume V_c = 242 m^3 as well as for two additional confined volumes, corresponding to $V = V_c/2$ and $V = V_c/4$. All the results for p_f are displayed in Figure A.6 for $V = V_c$ the maximum final pressure p_f = 160 bar at $y = m_c/m_m$ = 0.4.

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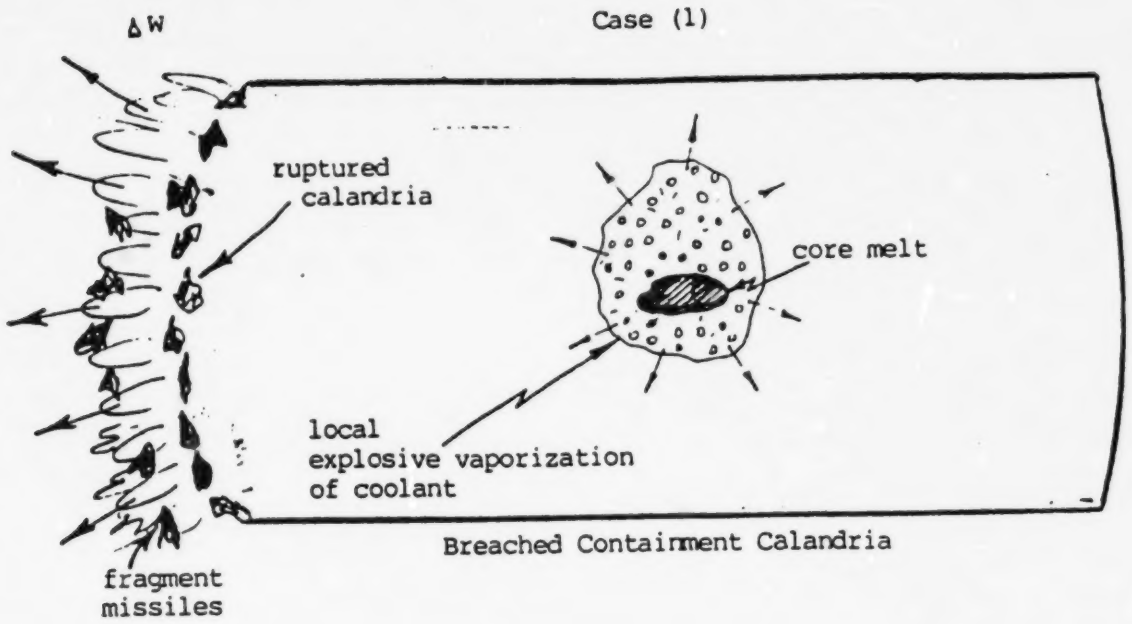
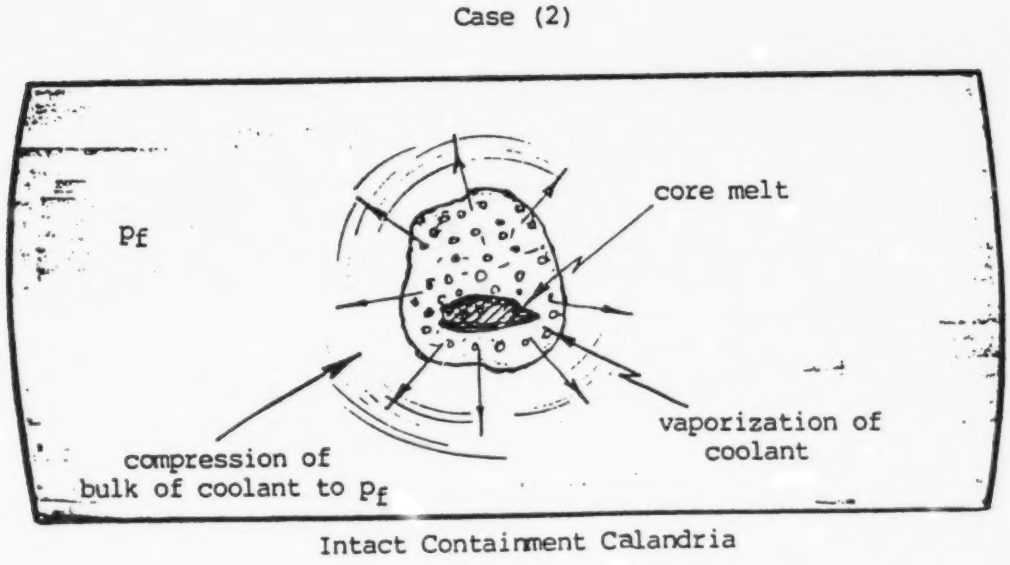
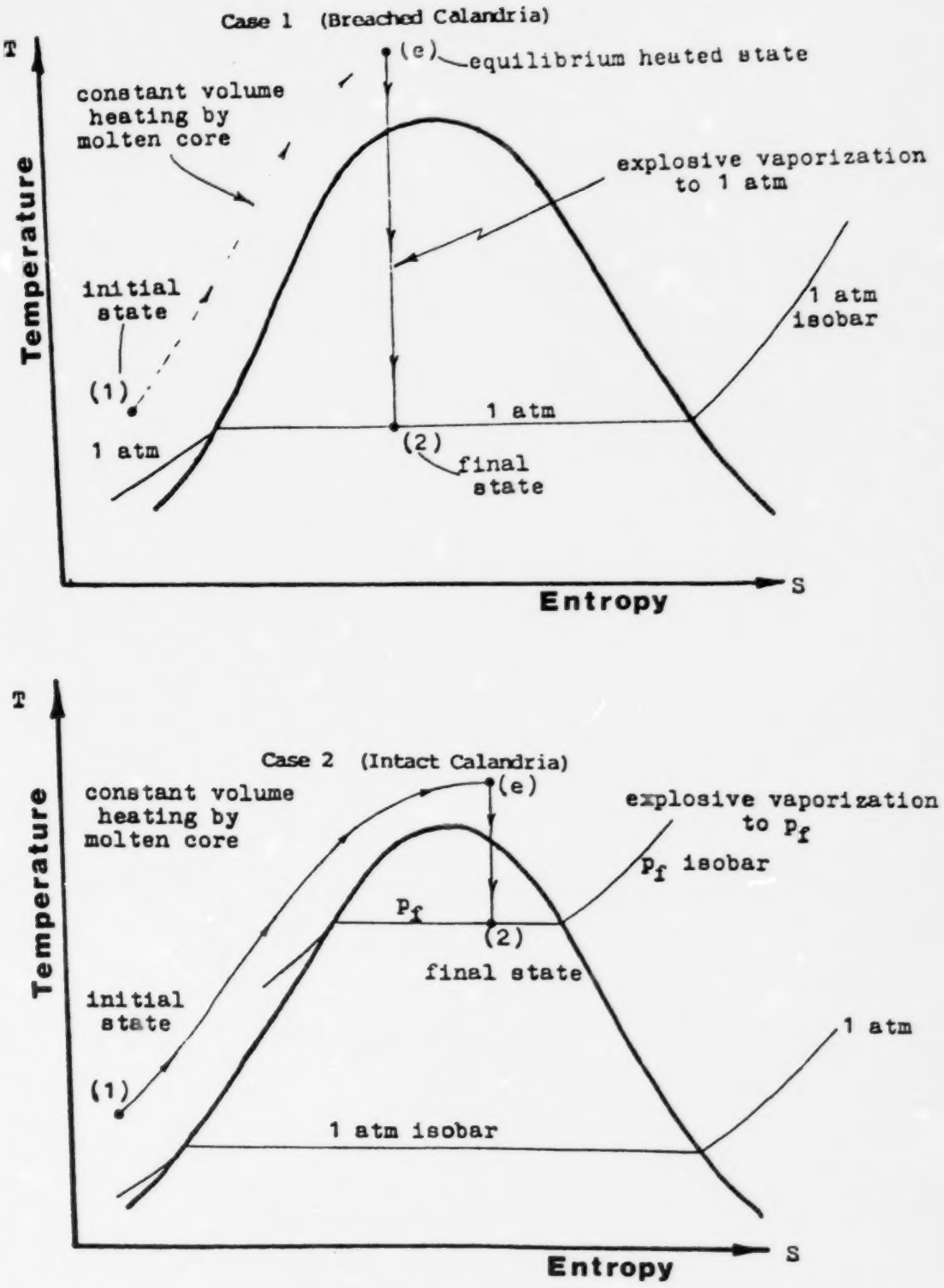




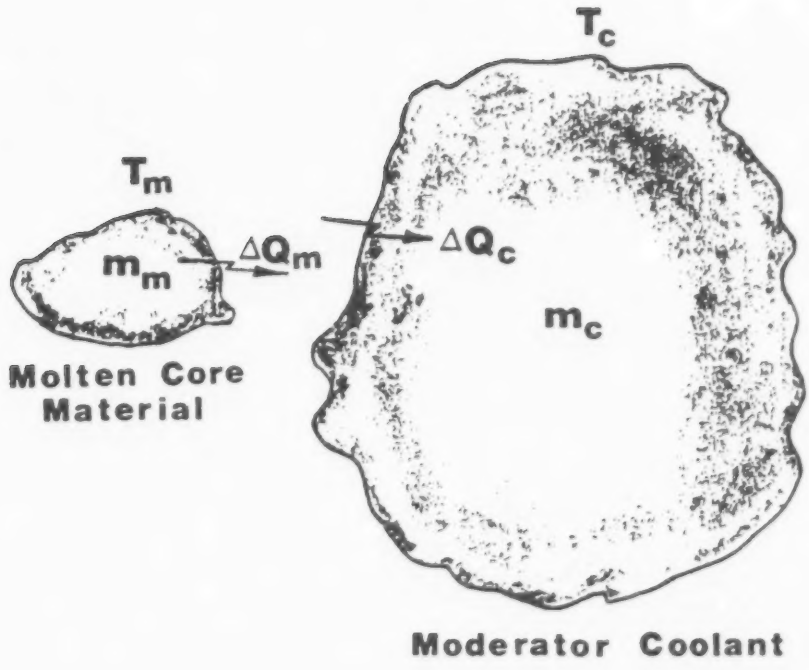
FIG·A·1

SCHEMATIC DIAGRAMS ILLUSTRATING STEAM EXPLOSION EFFECTS IN RUPTURED AND INTACT CALANDRIA VESSELS



FIG·A·2

LIQUID - VAPOR PHASE DIAGRAMS (TEMPERATURE ENTROPY) REPRESENTING COOLANT THERMODYNAMIC HISTORY FOR BREACHED AND INTACT CALANDRIA VESSELS



FIG·A·3

SCHEMATIC DIAGRAM ILLUSTRATING CONSTANT VOLUME HEAT EXCHANGE BETWEEN MOLTEN CORE MATERIAL AND AFFECTED MASS OF MODERATOR COOLANT

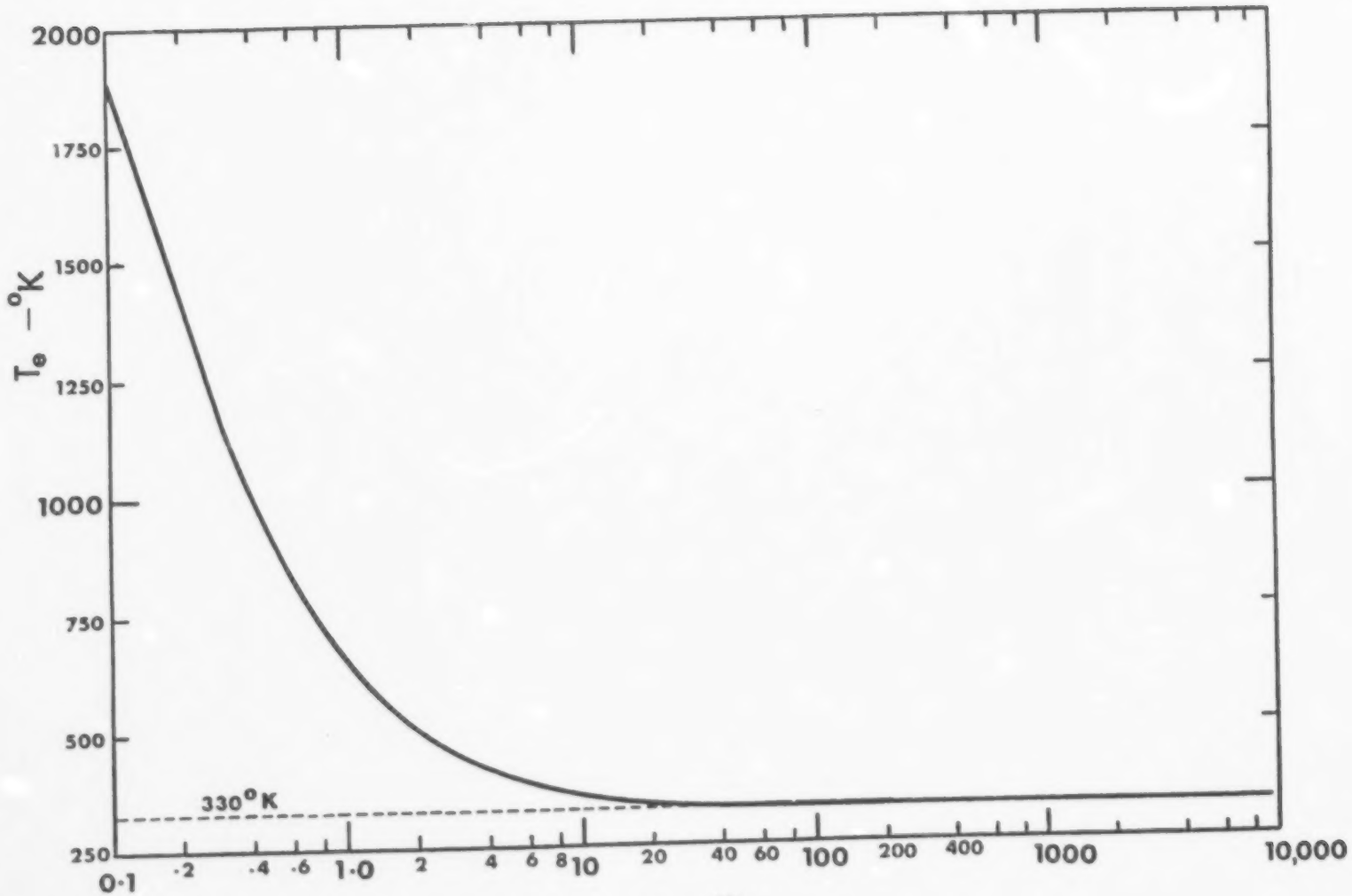
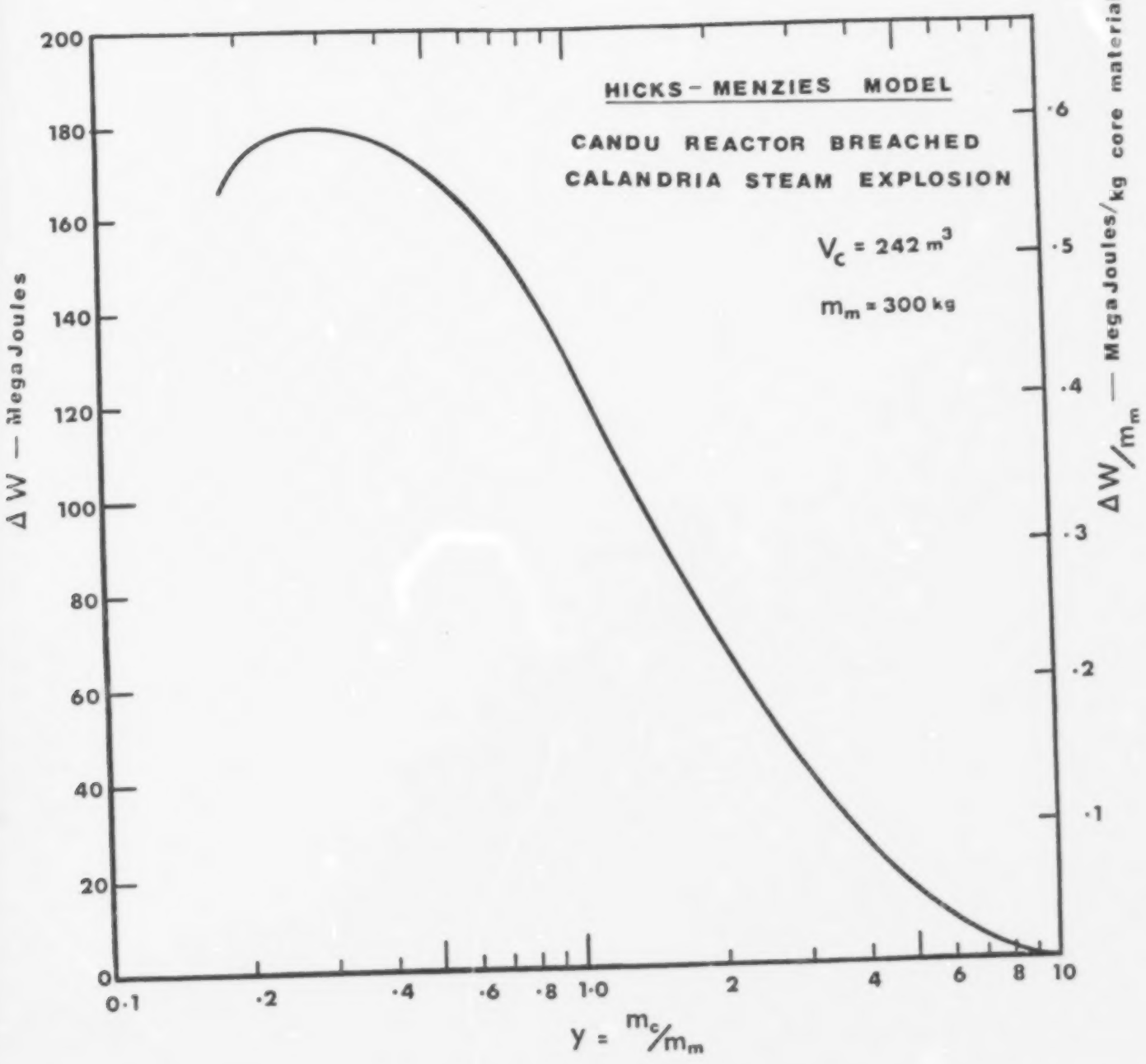


FIG · A·4

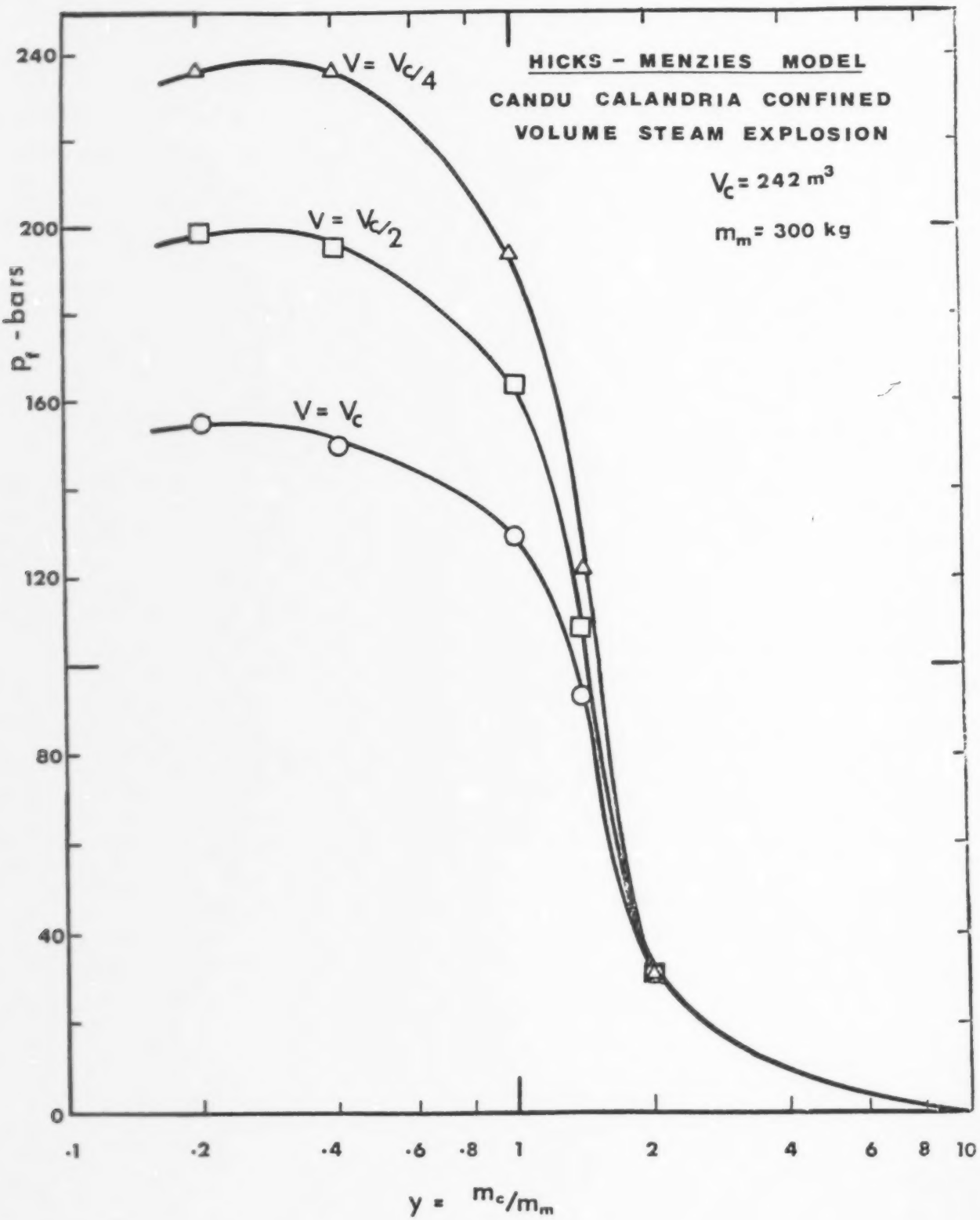
VARIATION OF EQUILIBRIUM TEMPERATURE T_e WITH COOLANT TO MOLTEN MATERIAL MASS RATIO



FIG· A·5

VARIATION OF THE STEAM EXPLOSION WORK POTENTIAL

ΔW WITH COOLANT TO MOLTEN MATERIAL MASS RATIO



FIG·A·6

MAXIMUM STEAM EXPLOSION PRESSURE P_f AS A FUNCTION OF THE COOLANT TO MOLTEN MATERIAL MASS RATIO